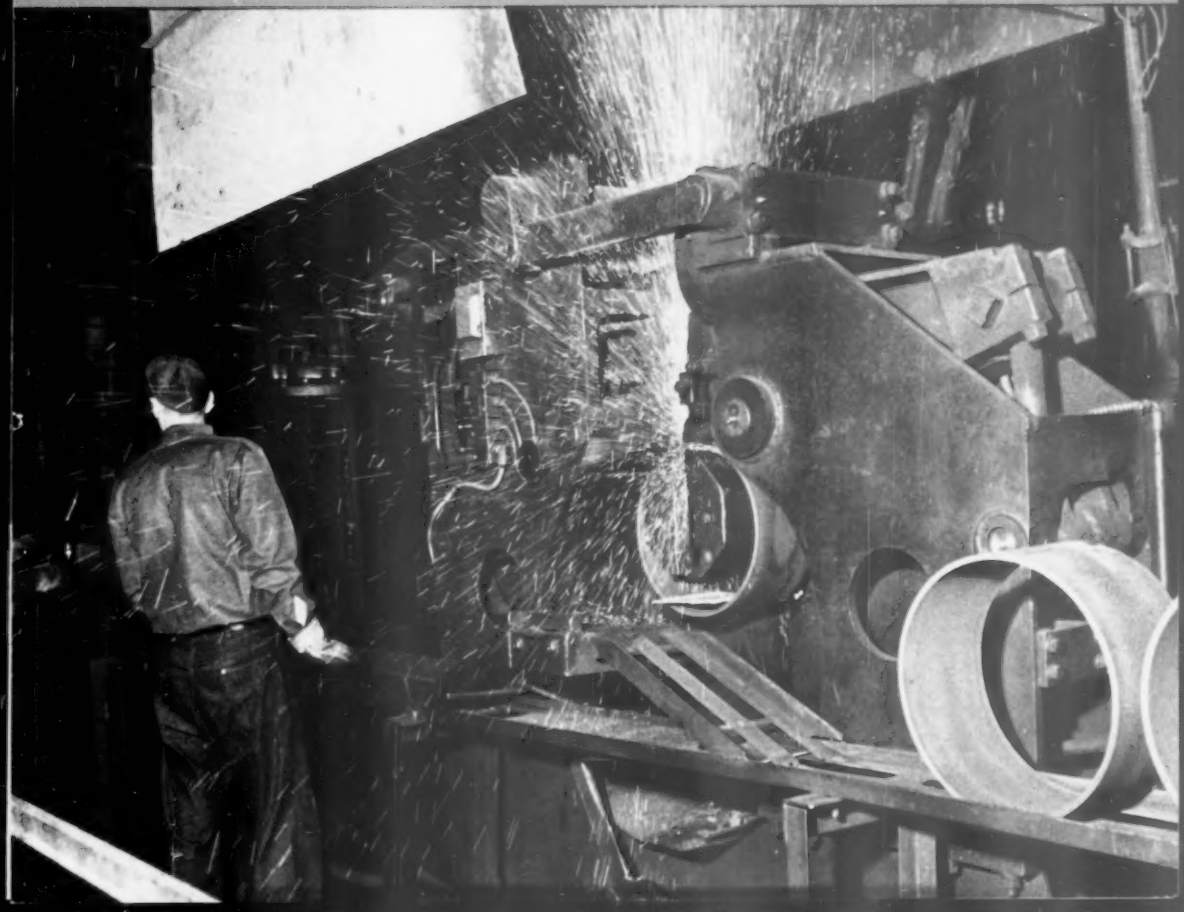
OCTOBER, 1949

The WELDING JOURNAL

Official Publication of the American Welding Society



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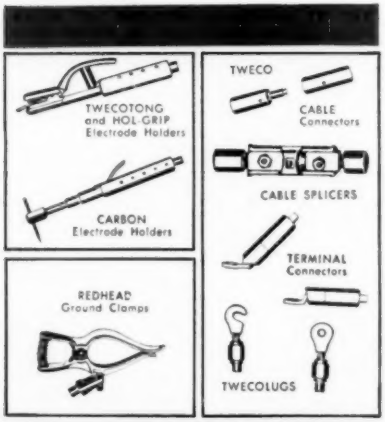

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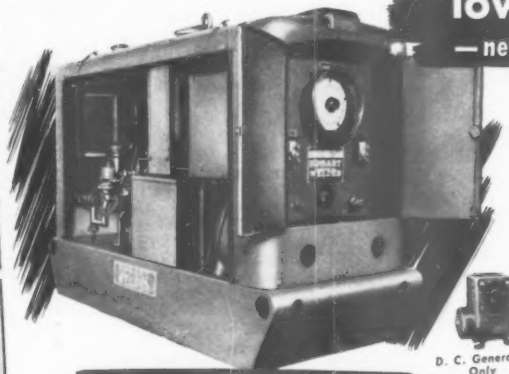

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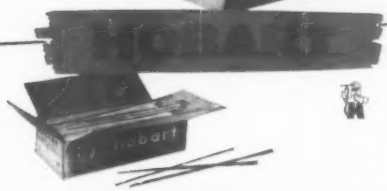

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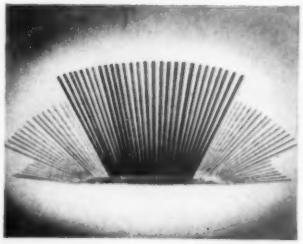

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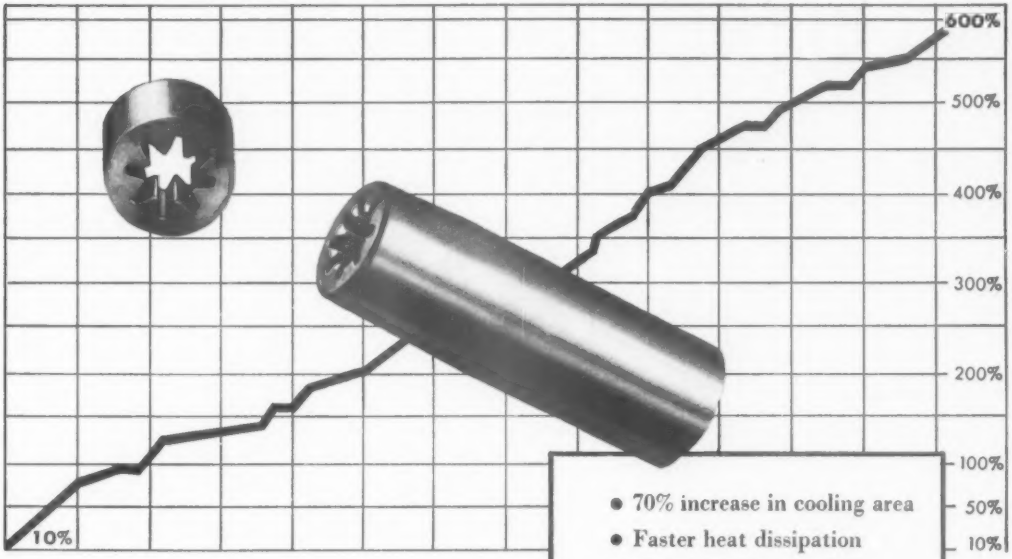

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Published monthly by the American Welding Society. Publication office, 20th and Northampton Streets, Easton, Pa. Editorial and general offices, 33 West 39th St., New York 18, N. Y. Advertising office, 400 Madison Avenue, New York 17, N. Y., Telephone: Plaza 3-4466. Subscriptions $5.00 per year in United States and possessions; foreign countries $7.00. Single copies, nonmembers $.75; special issues September and October $1.00; members 75 cents. Entered as second-class matter January 5, 1932, at the Post Office at Easton, Pa., under the act of March 3, 1879.

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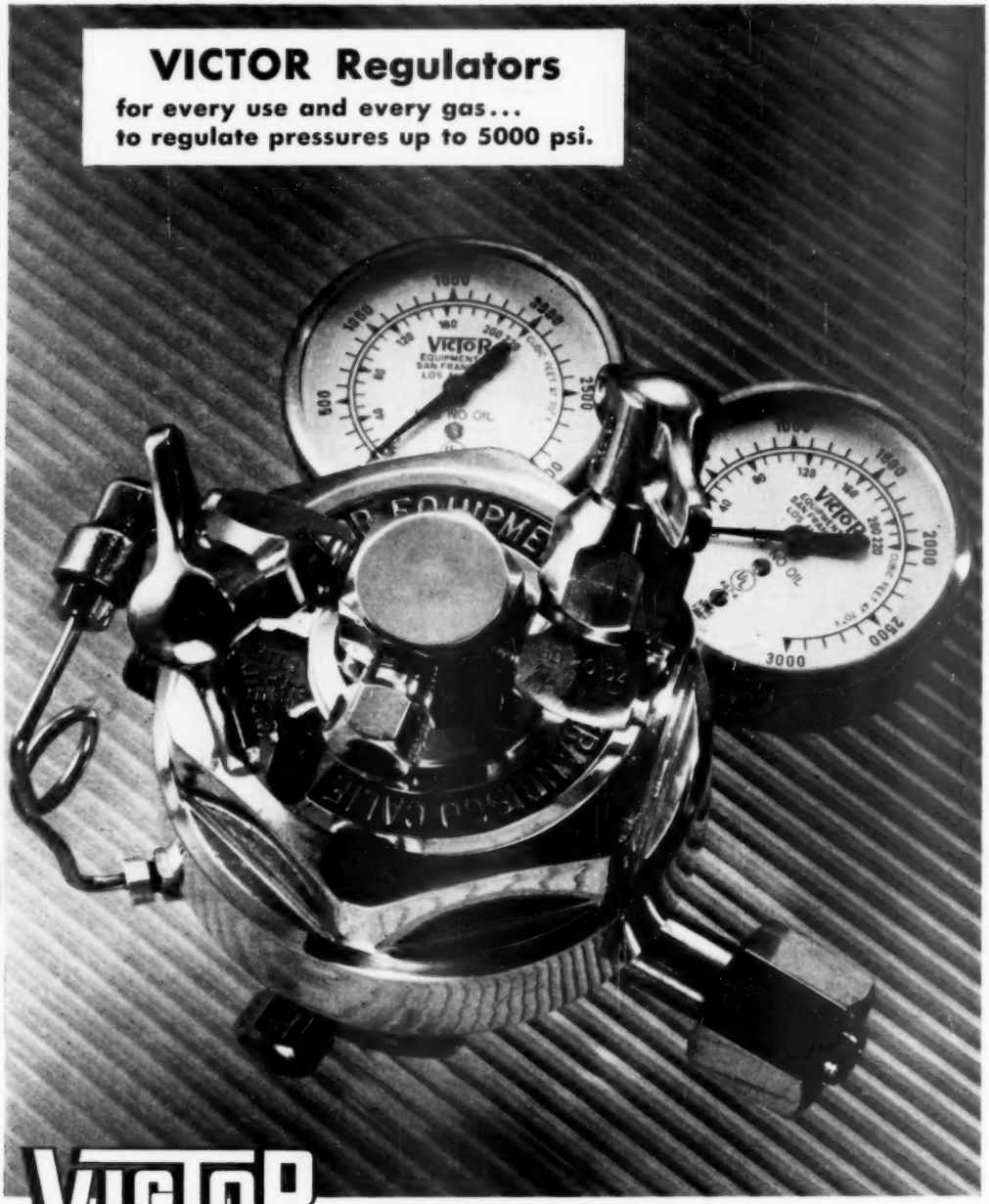

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The WELDING JOURNAL

volume 28 October 1949 number 10

Factors Affecting Quality of Production Flash Welding

Joints, materials, equipment tooling used in fabrication of bicycles and accessory parts are described and factors influencing quality of flash welds are described

by E. A. Sirabian

INTRODUCTION

DURING the past year over 600,000 bicycles were manufactured at the plant of Arnold, Schwinn & Co. in Chicago, Ill., a record volume for the industry. In the fabrication of these bicycles and accessory parts some 8,600,000 flash-welded joints were made.

The type of joints and materials involved, some features of the equipment and tooling used, problems encountered and results obtained with this production will be described in this report together with a discussion of the factors that have affected weld quality.

EQUIPMENT

At present, our flash-welding equipment consists of thirteen 50-kva., four 100-kva., two 150-kva. machines and a 100-kva. vertical-type machine not as yet in operation.

All of these machines are fully automatic, motor driven and cam operated. In each case air cylinders are used for clamping workpieces. Nine of the 50-kva. machines are equipped with springs to return platen. The remainder are hand return. The platens of the 100- and 150-kva. machines are all returned with air cylinders.

All of the 100- and 150-kva. and most of the 50-kva. machines are equipped with thermal and air pressure protection so that machines will not operate unless cooling water is flowing and air pressure for clamping is above a set minimum.

Current regulation is simply through tap switch provided on each machine. No attempt has been made to date to control line voltage. Contactors on all 50- and 100-kva. machines are magnetic. The two 150-kva. machines are provided with tube contactors.

Current cut-off in all cases but one is through current cutoff cam mounted on camshaft. In one case we use a timer to cut current off a few cycles after upsetting pressure has been applied.

WORK DATA

At present there are 12 flash-welded joints in our standard-line bicycle frame. Most of these are tubular cross sections. The head, however, is a flash-welded assembly of two stampings. The hanger is a stamping formed into a circle, then flash welded to close the ends. The rear fork end of the frame consists of stampings to which are flash welded the shaped tubular members which make up the upper stay and lower fork.

All of the steel used in the standard line frame members are S.A.E. 1010 steel.

We have different designs of front forks. Among the forks in which flash welding is used for fabrication, some are single-piece forgings to which the stem is flash welded. The others are forgings to which formed tubular sides are flash welded as well as the stem. Where a front fork is to be equipped for a lock, the stem consists of a lock bolt housing which is flash welded to a tubular piece. This assembly is then flash welded to the forging of the front fork.

The forgings are S.A.E. 1020 steel. The stems are either seamless S.A.E. 1010 or seamless 4130 chrome molybdenum steel depending on its application.

In our line of steel bicycle rims, we make three dif-

E. A. Sirabian is Welding and Design Engineer, Arnold, Schwinn & Co., Chicago, Ill.

Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

ferent styles of rims in three sizes each. These, of course, all have a flash-welded joint to complete the circular section.

Standard line rims are of S.A.E. 1010 steel. The stainless steel rims are type 430 stainless steel.

In our standard line of front and rear hubs there are six different styles in which flash welding is employed. In this latter case where flash welding is used as a method of fabrication, the parts are drawn stampings. Some hubs are simply two stampings flash welded together. Other hubs are drawn stampings in which other stampings or reinforcing members are inserted before flash welding. Hub stampings are formed from S.A.E. 1010 strip before welding.

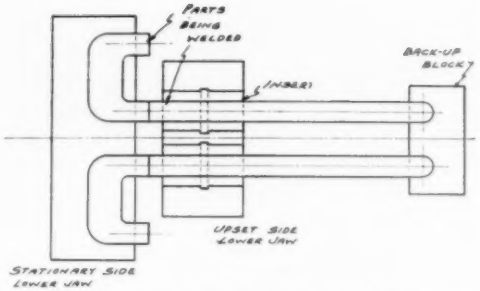


Fig. 2 Arrangement for welding two assemblies at one time

FIXTURE DESIGN

In the design of our flash-welding fixtures, we have made free use, where possible, of the rocker arm method of clamping. In this method, a pivot pin is employed between the clamping jaws and the operating cylinders. Where a straight line clamping motion is used, guide pins and bushings are employed to guide the movement. Both of these methods are proving very satisfactory for our purposes. They cost less to make than slides and gibs and are more trouble-free in operation.

The fixtures otherwise are simple fixtures employing air cylinders for clamping. In all cases we use back-ups for the workpieces. We do not rely on clamping pressure alone to prevent work from slipping.

It should be emphasized that, between the stresses set up in machine and fixture and the abrasive action of flash particles, flash welding is a most severe operation, probably more severe on machine and fixture than any other operation in the plant.

Accordingly, the simpler the fixture is in design, the more trouble-free the results will likely be. Of course, the fixture must be substantial enough to be free of undue deflection under load. All parts subject to rapid wear, such as workpiece locators, should be separate pieces as replaceable inserts and of suitably wear-resistant material. Further, fixtures must be easy to set up, operate and maintain.

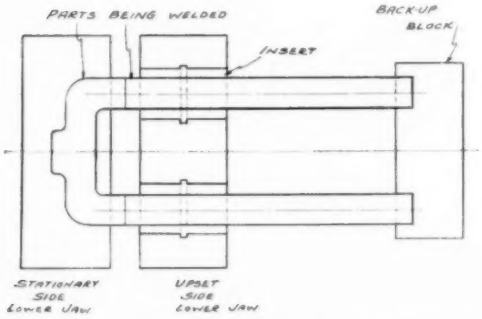


Fig. 1 Arrangement for welding two joints at one time

In the design of our fixtures, the attempt was made to standardize on the fixture parts as much as possible, particularly on the inserts. As mentioned, the inserts are substantially the portion of the clamping jaw assembly that serve to locate and back-up the workpieces. Hence, these are the parts subject to most wear.

We have done quite a bit of experimental work in an effort to determine the insert materials that stood up the best in production for the various jobs we have.

The fixtures, also, were designed to be as universal as possible. One fixture, for example, can handle six different designs of front forks simply by changing the clamping jaws and by changing the position of the back-up.

The need for careful design of fixtures for flash welding cannot be emphasized too strongly. If sufficiently high production cannot be reached, or downtime, and maintenance cost is excessive, the process itself is likely to be condemned, whereas the trouble may simply be in the design of fixture, in the layout of the procedure or possibly even in the design of the parts themselves for flash welding.

The ever-present flash particles present a problem. These particles accumulate not only on the fixtures but on vital machine parts, such as the transformer, ways and slides and the push-up cam mechanism as well. Too much thought cannot be given to the effective sealing against flash, sealing that does not interfere with ease of setup and operation.

It might be noted at this time that we have encountered no difficulty at all making two flash welds simul-

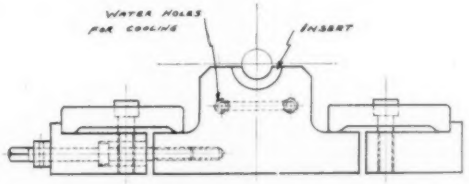


Fig. 3 Lower jaw—Flat clamping arrangement with provision for horizontal adjustment

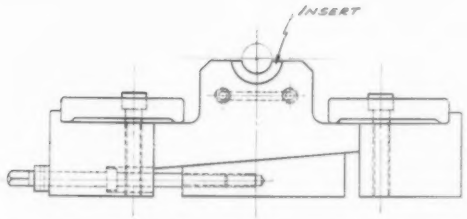


Fig. 4 Lower jaw—Flat clamping arrangement with provision for vertical adjustment

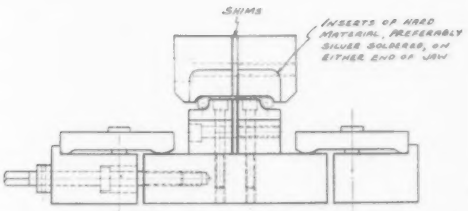


Fig. 6 Upper and lower jaw—Split jaw arrangement with provision for adjustment for width of contour and for horizontal adjustment

taneously. This applies to two flash welds on one workpiece or a single flash weld on two workpieces.

Figure 1 illustrates method of making two flash welds on one piece. Figure 2 illustrates the method of making flash welds on two separate pieces simultaneously.

It should be noted that from the standpoint of accuracy in alignment, it works out better to set up the job to weld two separate pieces as illustrated in Fig. 2, rather than make two welds on one piece. The reason for this lies in the relative ease of setting up and maintaining alignment with two separate pieces.

Some of our installations are shown in photographs, Figs. 7 to 12, inclusive.

Figure 7 is a photograph of our head halves stamping assembly. Production on this machine is 135 assemblies per hour. Figure 8 shows one of the installations where the fork end stamping is welded to the upper stay. Production here is 197 per hour. In Fig. 9 is shown the bottom bar to hangar assembly where the production is 271 assemblies per hour.

In Fig. 10 is shown the setup in which the back end of the frame is welded to the front harp. Production here is 100 assemblies per hour. Incidently, this is another installation in which two welds are made simultaneously.

Figure 11 is a photograph of the setup in which seven different assemblies of our front forks are flash welded simply by changing the clamping jaws and backup.

Figure 12 is a photograph of our rim welding setup. In this installation, three models of rims in three sizes each can be welded simply by changing the clamping jaws. Production on this setup is 360 pieces per hour.

WELD THEORY

At this point, it would be well to review the theory of the flash-welding process. As it is explained by our esteemed researchers, in the flash-welding process, a high magnitude of current concentrates in bridges of contact between the workpieces. The heat release is such that an instantaneous melting and partial vaporization of the bridge results followed by expulsion of the molten material.

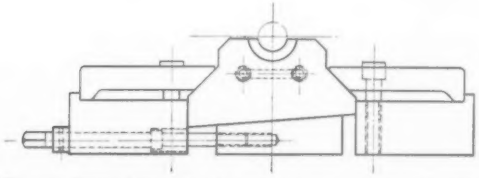


Fig. 5 Lower jaw—Angle clamping arrangement with provision for vertical adjustment

This action continues as one workpiece is moved toward the other. The resulting heat release causes a rapid rise in temperature of the workpieces. The action must be of sufficient duration so that a time is reached when the interface reaches probably the melting point of the material. In any case just beyond the interface the material must be at a welding temperature. This is the time just prior to the upset.

At this time the temperature at and in the zone of the interface is in the neighborhood of 2700° F. It has been established that at this temperature oxygen is extremely active. Metallurgists tell us that it can be expected that with mild steel the molten material of the interface will be covered with a thin layer of iron oxide. Further, that the oxides of silicon, manganese and phosphorus will be present together with the slag resulting from the other impurities in the parent mild steel.

Metallurgists point out that inclusions in the weld joint nucleate stresses which prestress the joint. This residual stress, then, is responsible for joint failing under application of a smaller load than could be carried were inclusions not present.

It might be said that the ideal flash weld is one where a "solid phase" type of weld has taken place. In his lecture on solid-phase welding, Dr. A. B. Kinzel* discusses the action that probably takes place in a solid phase weld. His explanation is that atoms are in constant motion in their crystal arrangement. Further, that under the temperature and pressure of solid-phase welding there is a movement of atoms across the interface. Thus, atoms leave one crystal arrangement and join others across the interface resulting in a bond.

In the ideal flash weld then, the workpiece welding faces have been brought up to welding heat simultaneously, the molten iron and slag has been squeezed out, all voids closed, a cross movement of atoms has taken place across the interface and no excessive disturbance of grain structure has resulted.

These conditions, resulting in a 100% strength weld,

* Kinzel, A. B., "Solid Phase Welding," THE WELDING JOURNAL, 23 (12), 1124–1144 (1944).

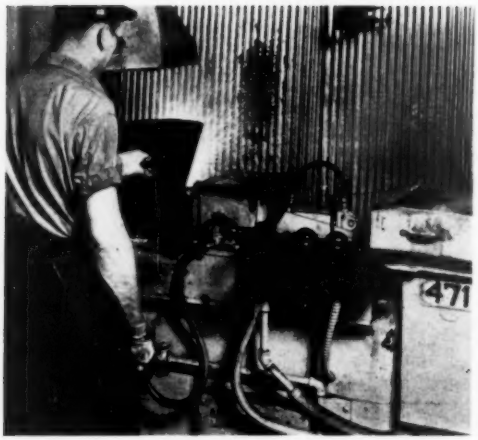


Fig. 7 Set-up for flash welding head halves stampings

are not at all difficult to obtain when setting a job up for welding mild steel. Continued results in production, however, is another matter.

Let us examine, then, flash welding results in production. As noted above, we judge weld quality on two counts, namely, on strength of weld and on accuracy of alignment of the workpieces.

Let us consider the strength of weld first. As noted above, maximum strength of joint results when workpiece welding faces come to heat simultaneously, slag and molten material is flushed out and voids closed, and grain structure is not altered excessively.

In our work we weld tubular sections to solid forgings, tubular sections to forgings with corresponding holes, tubular sections to tubular sections and solid sections to solid sections.

Fig. 8 Set-up for flash welding fork end stamping to upper stay

An important factor in bringing work faces up to welding heat simultaneously is the positioning of workpieces in welding jaws. In all of our work, we have encountered no trouble in this regard, using the published A.W.S. *Resistance Welding Committee Schedule for Flash-Butt Welding* as a guide.

With mild steel and the small cross sections covered in this report, the rate of heat input as determined by heat cam design does not appear too critical. We have used both constant velocity and accelerated rise in heat cams with no apparent difference in results for our work.

Another problem to be contended with is voltage vari-

Fig. 9 Bottom bar to hanger flash welding set-up

Fig. 10 Simultaneous welding of both rear forks to front harp

ation. Our supply line voltage changes during the working day due to conditions outside the plant. In addition, we have voltage swings within the plant which are due to the operation of our own equipment. Voltage swings on the order of ± 10 v. do not seem to affect results. However, at times, our voltage is in excess of this variation. At these times weld quality is no doubt affected. Just how far voltage can be permitted to swing before weld quality is affected is a factor that we have not as yet had time to determine for our class of work.

Inasmuch as practically all of our work involves small areas, from 0.08 to 1.25-sq. in. cross-sectional area, and in no case exceeds 9 sec. flashing time, we have done no work toward minimizing flashing time.

An important factor in production work in the amount of heat input is the quantity of flash that ac-

Fig. 11 Universal set-up for flash welding any one of seven different front fork assemblies

Fig. 12 Universal set-up for flash welding bicycle rims

cumulates on machine and fixtures, and the oxidation of current-carrying contact surfaces of machine and fixture. While operators clean their machines at the end of each work shift, they cannot be expected to remove flash and oxide between current-carrying contact surfaces, particularly in periods of heavy and sustained production.

As a result,more and more secondary voltage is consumed in the increased resistance set up in the secondary circuit. The point may be reached when the secondary voltage is not sufficient to sustain flashing action and a cold weld results. Swings in line voltage which are of course more severe in times of high production, cause an erratic action in this regard. For example, a surge would possibly compensate for increased resistance, and might be sufficient to cause flashing action, whereas a drop in voltage would probably result in a dud.

For this reason, it is of the utmost importance to set up a schedule of maintenance so that, at regular intervals of production, fixture and machine are disassembled, cleaned thoroughly and worn parts replaced.

TIMING OF UPSET PRESSURE

It has been stated that the most important single factor in flash-weld strength is the timing of current cut-off and application of the upset pressure. Our experience is entirely in accord with this statement.

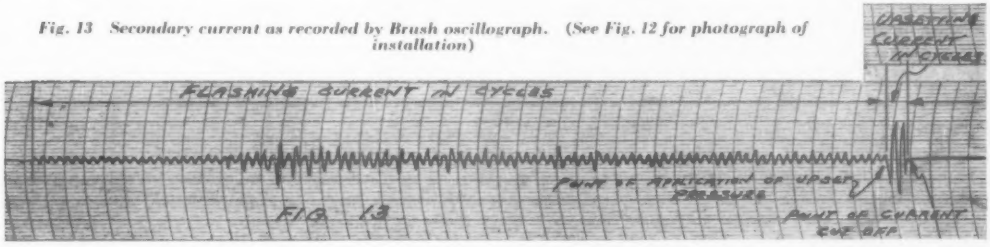
As noted under theory of flash welding, assuming that workpieces have been properly and simultaneously brought up to welding heat, the success of the weld with mild steel is dependent entirely on the ensuing action, namely on the timing of the current cutoff and the amount and speed of the upsetting action.

Up until the present time we have been checking our timing in the conventional manner. This is done by setting machine so that current is cut off just as the upset pressure is being applied. With this as a starting point, the point of current cut-off and the amount of upset pressure is varied slightly until the maximum strength is attained. It seems generally agreed that with mild steel and average application it is best to allow current to remain on a matter of a few cycles after upset pressure has been applied. This is in the nature of insurance against oxidation in the joint due to too early a current cutoff.

Incidentally, we judge strength of joint simply by bending or twisting assembly until failure takes place. Failure must be outside of weld. While this method of timing has served in the past, it must be considered inadequate for the requirements of progress. It is too time consuming and hit or miss.

Instruments are now available to aid in establishing and setting point of current cutoff with respect to application of upset pressure. These instruments are oscillographs on which can be recorded both platen movement

Fig. 13 Secondary current as recorded by Brush oscillograph. (See Fig. 12 for photograph of installation)



and flash-welding current in cycles. Thus, the best point of current cutoff with relation to application of upset pressure can be determined for any given job. It would then be a relatively simple matter to duplicate this setting when making a set-up for a new run or for checking a long run of production.

Figure 13 is an oscillogram of actual secondary current taken on a Brush recorder during the welding of a bicycle rim. (See Fig. 12 for photograph of rim-welding installation.) Bicycle rims are tubular sections on the order of about 0.125-sq. in. cross-sectional area. The machine shown is a 50-kva. Taylor-Winfield unit.

Note points of application of upset pressure and current cutoff as shown by increased secondary current.

In all cases, where upsetting current is used, namely where welding current is carried for a matter of cycles into the upset before being cut off, oscillogram can be used to check timing of current cutoff. This is true because upsetting current is several times larger than flashing current, hence, readily shows up on the graph.

In these cases, timing of a flash-welding machine can be checked quickly and conveniently without resorting to a parallel record of platen movement.

There is one other factor that instruments should record or indicate. That is the amount of upset pressure developed. This, of course, is a measure of the plasticity of the material at time of upset. We understand that Dr. W. F. Hess is now engaged in making a study of this factor.

It has been established that oxygen is extremely active at the temperatures of welding, forming oxides in the interface instantaneously. While an inert atmosphere around weld area is not essential to the making of a good flash weld, it would stand to reason that an inert or reducing atmosphere would make the operation less critical.

We have done some work in this connection, using argon and city gas. However, we were not able to affect a good seal around the weld area and no improvement in weld quality was discernible. We plan to do more work along these lines in the future as time permits.

When weld is made between two tubular members, such as pipe or tubing, inert atmosphere can be readily set up.

One such installation is at the Babcock and Wilcox Tube Co. at Beaver Falls, Pa.[†] H. D. Newell, chief metallurgist, advises that an atmosphere of hydrogen gas is used as a regular procedure when flash welding alloy tubing. While hydrogen is now being used, the use of an inert gas such as argon or helium is being considered for reasons of safety.

It is important, of course, that fixtures and machines be checked regularly to make certain all parts are tight and that workpieces do not slip under application of upset pressure.

With mild steel, not enough change in grain structure takes place to require any preheating or postheating operation.

With one of the assemblies however, we weld a length of chrome molybdenum tubing to a 1020 steel forging. Inasmuch as weld results in brittle structure, we follow welding with an annealing operation to develop necessary ductility.

One of our machines is equipped with an annealing station. In this setup, after assembly has been flash-welded, a reduced current is passed through the joint to refine grain size. This arrangement works out well as only a few seconds are added to the required time for each piece.

WELD ALIGNMENT

As covered under fixture design, care was used in selecting clamping methods, areas and pressures to hold workpieces rigidly. Further, in no case do we rely on clamping force alone to prevent work slippage under upset pressure. In all cases, both pieces are backed up either by the welding jaws themselves or by independent backups.

While high clamping forces insures better rigidity and electrical contact, particularly with forgings, by the same token, increased jaw wear results. In all of our fixtures, inserts are provided at the points of most rapid wear. These points generally locate the workpieces, hence, wear must be watched to maintain alignment.

In all cases we have made provision for compensating for wear, by horizontal adjustment of clamping jaws on one platen and vertical adjustment on the other. Figure 3 illustrates provisions for horizontal adjustment and Fig. 4 for vertical. Figure 5 illustrates another type of holding clamp from that shown in Figs. 3 and 4. While we prefer the type shown in Figs. 3 and 4, both types work well.

When it is necessary to align a double thickness or a contour, a third adjustment, one for width of contour, has been found advisable. This is illustrated in Fig. 6. The split portions of the jaws are simply adjusted in width with shims until the desired line-up is secured. After a period of wear, process can be repeated until a dressing of the contour becomes advisable. With this design we find it possible to obtain a very satisfactory alignment of workpieces.

Note the inserts placed at the points of maximum wear. While there is no question but that the jaw blocks proper should be a copper base alloy for reasons of conductivity, the inserts themselves do not necessarily need to be copper base materials. We are using inserts of Elkonite TC-10 that were silver soldered into the jaw blocks with most excellent results. We have also used cobalt base alloy material for inserts with good results under certain conditions. With this latter type insert it is essential that stock to be welded is clean. Flash particles burn on and adhere to insert faces if not cleaned off and thus the good locating surface of the insert is soon damaged.

† *Technical Bulletin No. 7A.* The Babcock & Wilcox Tube Co., Beaver Falls, Pa.

It should be noted that for satisfactory results, the Elkonite insert materials must be properly supported or better yet, silver soldered in place.

In all cases it is desirable to water cool the jaws. Electrical conductivity drops rapidly with increase in temperature. With a conductivity of 100 at room temperature, the conductivity of copper drops to 75 at 200° F. Furthermore, flash is much more readily cleaned off a cool surface than a hot one.

In connection with the use of Elkonite type inserts, it appears to us that better results still would be secured if the insert material were more resistant to wear than the material now available even at a much lowered electrical conductivity.

In all cases, it is highly desirable to keep jaw surfaces clean of flash and dirt. Inasmuch as operators wear gloves, it is a simple matter for the operator to wipe surfaces off with gloved fingers, as necessary, when job contour is accessible. When contour is not readily accessible, a blast of air for cleaning should be provided.

Where possible, air holes should be provided in jaw blocks themselves. We have a very satisfactory installation with air holes in lower jaws only. In this case, when clamping cylinders operate to unclamp the work, the air from the cylinders is exhausted through the air holes in the lower jaws. This blast does an effective job of blowing flash particles off jaw surfaces.

One of our installations has given remarkable results in maintaining close, trouble free alignment over a long period of time. After some 300,000 flash welds with one set of inserts, the alignment is still being maintained within the exceptionally close limits of this job. A recent examination of the inserts by our gage laboratory showed some errosion due to flash, but practically no wear of the locating surfaces.

The insert contours of this job are such that the operator readily can and does keep insert surfaces scrupulously clean. Further, the stock being welded is exceptionally clean.

While the criterion of alignment is in the welded assembly itself, dummy gages can be used to good advantage in setting a job up and for checking alignment. Also, finished surfaces on the fixture or clamping jaws themselves on either side of the weld line, can be used for checking alignment with a straight edge or with an insulated indicator.

Due to the scoring action of the flash and the temperature and high stresses involved, machine and fixture parts will in time loosen and wear. Thus, alignment may be thrown off, and the timing of the current cut-off affected. For these reasons it is highly important to have a schedule of inspection and maintenance of machine and fixture.

Cleanliness of the workpieces themselves pays dividends. While the regular shop run of oil, dirt and rust do not apparently affect weld strength, they do affect the cost of the operation through increased maintenance. All such matter foreign to the material being welded tends to contaminate the jaw surfaces causing greatly increased wear. Thus maintenance cost can be minimized by the use of clean stock.

SUMMARY

High levels of production have been reached and maintained. To attain and hold these levels, machines were made, fully automatic and suitably safeguarded. The fixtures are simple in design and heavily built.

Accurate timing of current cutoff is essential to weld strength. Inclusions, cracks and porosity lower joint strength. Check on timing in the past has been maintained satisfactorily in the conventional manner. However, an instrument on which could be recorded energy input, timing of current cutoff and amount of upset pressure reached, would no doubt serve as a more accurate and time saving check on weld quality.

Maintenance of close alignment has been possible through provision for jaw adjustment, horizontally, vertically and for width of contour. Jaws are all water cooled and in some cases equipped with scavenging air blast. At points of maximum wear, jaws have been provided with replaceable inserts of suitably wear-resistant material. A regular schedule of inspection and maintenance of machines and fixtures, together with the electrical and air systems, has been found necessary to keep up weld quality and alignment.

ACKNOWLEDGMENTS

The author wishes to express his gratitude to Arnold, Schwinn & Co. for making this paper possible. Also his sincere thanks go to the personnel of the Taylor-Winfield Corp. and the S. M. S. Corp. for their assistance at various times.

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Design of Welded Rigid Frame Subways

Welded rigid frames for proposed new 250 subway track miles would save New York City $10,000,000. Their fabrication can be geared to mass production methods

by Martin P. Korn

INTRODUCTION

NEW York City has projected for early construction new rapid transit subways aggregating about 250 track miles. The principal line of from four to six tracks will follow Second Avenue in Manhattan Borough. Authorities have publicly declared the urgent need for these subways and efforts are being directed toward securing the necessary funds. Legal obstacles alone are temporarily delaying their acquisition.

Because of this need for funds, improvements making for economy would be both timely and important. Following the spectacular record and progress made by welding during the recent war vast savings in subway costs can be achieved by the substitution of welded rigid frames for the standard and conventional riveted bents. Those savings would total 40,000 tons of steel and $10,000,000 in costs. This is a simple statement of fact determined from exhaustive studies.

An official investigation probing the use of welded rigid frames reported them 100% technically sound and effective toward large reduction in steel tonnage.

Representing the optimum of modern engineering design and employing the latest developments in the science and art of welding, welded rigid frames for subways can be geared to mass production methods affording vast economy without precedent in structural fabrication because of the magnitude of this project.

New York City will profit thereby immediately. Dreams of other cities for similar rapid transit will be brought closer to reality because of these decreased costs.

Welding and steel will reach out toward new horizons and a new future with markets formerly nonexistent. Wherever concrete subways have already been planned for economy or other reasons, Big Steel can show its metal competing for the first time against all-concrete construction.

The writer has studied the subject intermittently over a period of 30 yr. Then followed 10 yr. more of efforts tending to prove that vast savings would result from use of welded rigid frames. In an effort to reduce this rigid frame saga of 40 yr. down to a 40-min. talk, a spotlight will be focused right through time onto those facts which it is believed will be of specific interest and value to this audience of national scope.

IN THE BEGINNING

During 1910 and 1911 the writer had charge of construction of a part of the Fourth Avenue Rapid Transit Subway in Brooklyn, N. Y. It was a four-track continuous reinforced concrete structure. Figure 1 is a reproduction of one of its typical sections. Massive in its proportions of roof, wall and floor construction, the type of design stirred my imagination and that of several co-workers not long out of college. Our job was to build and not to question why. Yet we could not help wondering how those big and important brackets influenced the stresses in the structure.

Lacking official backing our quest for knowledge found a route. Special inserts and nails were clandestinely planted, elevations were taken and locations were recorded. After backfill had been completed—and even before then—results were investigated. No deflections developed in the roof, none in the sidewalls—none that we could detect with a Y level and our other primitive research equipment. Time swallowed and scattered that small group of curious young engineers. The writer steered westward, but that rugged continous concrete structure never left him in any of his travels. Those brackets took root early to grow and develop in mutual friendships through experience and tests down through the years to this day into the all-welded steel rigid frame subway bents now proposed.

The term "rigid frame" was not in common text book usage back in 1910.

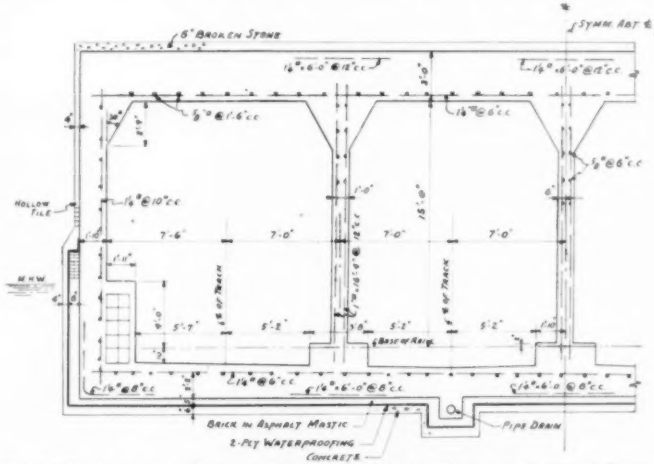

Fig. 1 Typical section between Sta. 184 + 76–187 + 11 Fourth Avenue Subway, Brooklyn, N. Y., May 16, 1910

Martin P. Korn is a Consulting Engineer, Dearborn, Mich.

Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

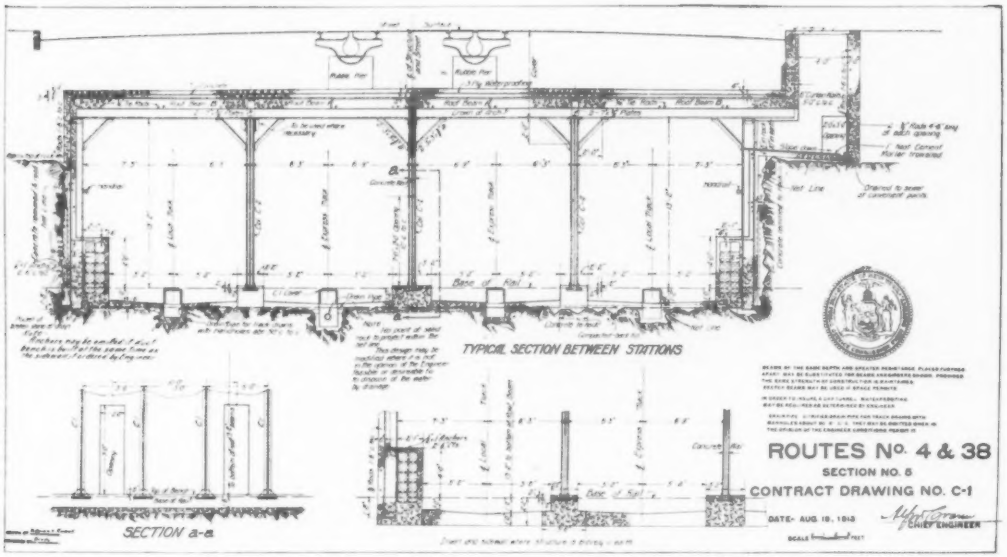

Fig. 2 Seventh Avenue Subway between 17th and 30th Sts., Manhattan

We classified the above structure in Fig. 1 as a hybrid arch, but unlike an arch it had huge brackets and reversal of stresses. Today it would plainly be classified as a rigid frame by virtue of its form, its structural behavior pattern and its reinforcing. That concrete rigid frame subway was the only one of its kind built in New York.

Figure 2 is a subway section built shortly afterward in 1913. Comprising articulated riveted steel bents spaced 5 ft. on centers separated by concrete jack arches, it represented standard subway construction of that period dating back to the first subway in New York built in 1901.

Coming down the years we see in Fig. 3 a subway built in 1931, very little change from the type of the original design. It no longer had knee braces, they had been eliminated in the Twenties. A slight change had also been made in the column connections.

Figures 4 and 5 are views of subways built in Brooklyn in 1940 and, as you will observe, they differ little from those views of the earliest construction. Other than some concrete arches for tunnels the standard type of subway design remains today almost as it was in the beginning, articulated steel riveted bents separated by concrete jack arches.

Since a new type of subway construction is at last being proposed, it should be of interest to have at least a general outline of the loadings that determine all designs. They will be presented briefly.

SUBWAY DESIGN STANDARDS

The Engineering Dept., Board of Transportation, is charged with the design and construction of all New York City subways. All work is done by their own personnel. Design is in accordance with their own standards independent of other city bureaus. Where buildings are involved directly the City Building Code is followed.

Fig. 6 presents diagrammatically the subway loadings. The design is as follows:

Steel bents are spaced 5 ft. on centers.

Dead load on subway roof per square foot is the product of the height in feet from underside of roof beams to the street surface (known as cover) multiplied by 100 lb. Roof live load from city streets is 600 lb. per sq. ft. where cover is greater than 9 ft.

Roof beams are designed as simple spans to carry the full live and dead loads. When the vertical column load is less than 150,000 lb., one-half of the load is considered as carried by the sidewall concrete.

Where bottom of subway is below water the invert beams are designed for the full dead load of structure omitting only the street live load.

Shear and bearing of beams directly at columns includes both the full live and dead loads. Stiffeners are used throughout.

Column base and cap plates are either riveted or welded.

All steel work in shop or field is riveted.

Unit stresses, in pounds, per square inch:

Tension on net sections	20,000
Compression for L/r not greater than 3	20,000
Subway columns 20,000–95 L/r; Max.	16,500
Bending	20,000
Shear on webs	12,500
Bearing (web)	20,000

Allowable bending stresses are increased

Fig. 3 Route 108, Queens Blvd. and 65th Rd., Queens Borough, Sept. 19, 1930

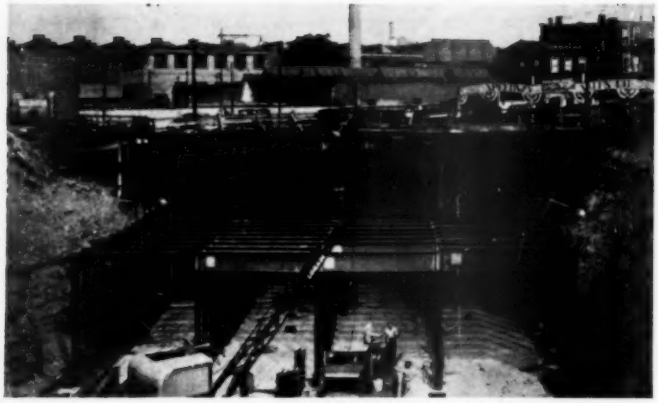

Fig. 4 Route 110, Jamaica Ave. and Fulton St., Brooklyn, Aug. 9, 1940

to 25,000 lb. where beams and girders are encased between the concrete jack arches.

1925—RIDGWAY-RAISMAN—1938

An accidental meeting at a hotel in Kansas City in 1925 with Robert Ridgway, Chief Engineer, New York Subways, led to renewal of an old friendship. He had just returned from a Western meeting of the American Society of Civil Engineers of which he was President. I was doing some work in the southwest. In answer to his jocular question as to whether I had forgotten completely about subways my answer was a decisive "no." "In fact" said I, "I am dreaming of a welded rigid frame subway which will save at least 20% in weight of steel, considerable excavation and millions in costs. Shop fabrication would be reduced to mass production of identical welded corner brackets and interior column brackets. Field welds would be kept to a minimum."

Everything in that terse description appeared provocative except the word "welding" which for just a moment transformed his usually warm voice into what seemed like a chilled gasp of fright. Such was the reaction to welding then by that nationally famous engineer. This brief historical anecdote is mentioned because of what occurred 12 yr. later in 1937.

Robert Ridgway had already been retired and become a Consulting Engineer. My studies and research had advanced successfully to a point where it seemed opportune for me to interest him in the possibility of association as Consultants on my plan of welded rigid frame subway construction first mentioned to him in 1925. The psychological question of how to convert him to welding was of course my first objective. But that was no longer necessary.

The Lincoln Vehicular Tunnel under the Hudson River was then under construction and welding was being used throughout on certain land approaches. Robert Ridgway was Consultant on that project. His confidence in welding was now being based on reality, on the living structure. Moreover, his knowledge of welding was plainly manifested by that blueprint sent me of the detail shop drawing showing the continuous and large circumferential welds which, by their size, seemed to exude visual pride in their importance—welding done for economy and added strength! Those were the reasons advanced then by Chief Designing Engineer Ralph Smillie. (This drawing was reproduced in a brief article on that work by the writer, which was published in THE WELDING JOURNAL in May 1937.)

Could there now be any doubt about welded rigid frame subway bents with regard to the matter of welding? That hope of an association was but a short-lived dream for death suddenly stilled the voice of that venerable and famous engineer.

Contact was forthwith established with A. I. Raisman, Chief Designing Engineer, New York Subways, who manifested immediately a warm interest in my proposed design for welded rigid frame subway bents. It was my first meeting with him since 1925 when I first broached the idea

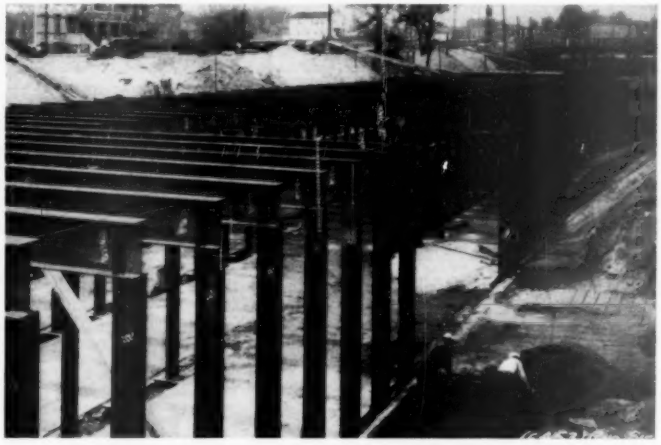

Fig. 5 Route 110, Sutter Ave. at Sunrise Highway, Queens Borough, May 9, 1941

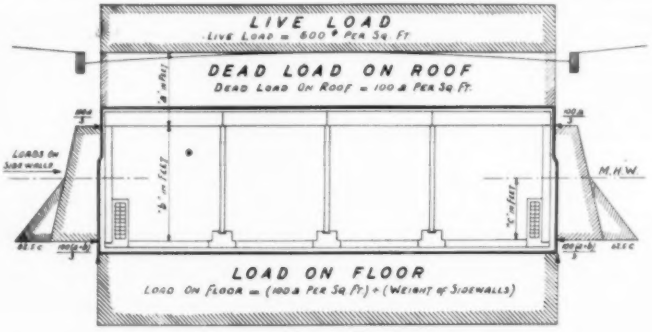
LIVE LOAD
LIVE LOAD = 600 # PER SQ. FT.
DEAD LOAD ON ROOF
DEAD LOAD ON ROOF = 100 # PER SQ. FT.
LOADS ON SIDE WALLS
M.H.W.
LOAD ON FLOOR
LOAD ON FLOOR = (100 # PER SQ. FT.) + (WEIGHT OF SIDEWALLS)

Fig. 6 Subway loading diagram

and at which time he gave me his autographed book of subway design standards which was to serve me in my research throughout the ensuing years. Remembering well his interest in my proposed plan from the beginning, I wrote on Feb. 10, 1938, offering to donate to him officially all my research and design data on welded rigid frame subway bents. It was my hope to be commissioned as Consultant. He sent me immediately blueprints of a completed subway contract with all the cost data for comparison with my proposed design. Several months later Mr. Raisman passed away.

Thus did death claim in quick succession not alone two of the most illustrious engineers of subway design and construction but also the two engineers whose interest and moral support for the design would have carried the greatest weight. It was not alone the idea of rigid frames—it was their willingness to consider welding on its merits and performance, a factor frequently ignored by older engineers even to this day.

To keep my design from falling into unscrupulous hands inimical to its successful execution, I yielded to advice and applied for a patent which was granted in June 1940.

Today, with the Second Avenue subway construction now in the offing and the matter of funds its only stumbling block, my patented design with its compendium of development and research data will once again be offered as a gift in the public interest to New York City in the same spirit in which it was originally undertaken with Robert Ridgway and A. I. Raisman.

RIGID FRAME—RESEARCH AND DESIGN

For the benefit of welding specialists and other engineers who are not too familiar with this branch of engineering, a thumb-nail sketch of this type of structure will be given.

The rigid frame is a development of modern design providing new structural form with special advantages. Its main advantages are economy, esthetic form and savings in headroom.

Take a pencil, sketch a beam, bend it, add sufficient strengthening material at the bend to obtain full continuity there, and you will have an elementary rigid frame. Two operations made it a rigid frame: the change in direction of its neutral axis, and the addition of sufficient strengthening material at the bend to obtain continuity.

For a simple definition I would say that the rigid frame is a structural unit with one or more continuous joints of members whose axes are discontinuous. That definition holds whether the structure is made of concrete, wood or any metal. The continuous joints are called knees or brackets.

Between the years 1925, when study for a design was first undertaken with the guidance of Chief Designing Engineer Raisman of the New York Subways, and 1938 when its initial presentation occurred, no effort or expense was spared for thoroughness in investigation and study. The problems investigated reached far and afield and considerable data was obtained directly from construction men.

Designs were made of from 2 to 4 track sections and studies of double-deck sections. Experienced stress analysts of indeterminate structures were consulted in different locations in order to obtain the widest concept of the best design procedures.

From the beginning of my research no information existed anywhere on stress behavior or stress analysis of the knees in rigid frames. Designs that were made in those days (the Twenties) depended more upon the designer's ability to guess correctly and on his good judgment than on definite and established design data. Professor Swain, who was recognized for his ability to probe indeterminate structures, exclaimed that no one could tell me how to design those knees. And in 1927 when I designed my first steel rigid frame for an industrial plant, I too did some tall guessing although my work was considered highly technical. It was then that I began to plan to institute tests of models to determine the stress behavior pattern in knees and from that perhaps establish procedures for their design. Such tests were finally conducted by me privately, and at last it seemed to me that the missing technical link in a planned technical chain of progress had been accomplished. (Some of these results were published in THE WELDING JOURNAL for January 1940 in an article which I wrote on "Design of Welded Rigid Frames.")

Meanwhile in the late Thirties the U. S. Bureau of Standards in conjunction with the American Institute of Steel Construction instituted a program of tests on riveted and welded rigid frame knees. The University of Illinois came forth with illuminating tests on concrete knees in rigid frames. Lehigh University, the University of Washington and still other leading schools conducted further tests and research so that today considerable data is available for both the academic-minded and the practical designer.

Design of rigid frames for buildings is common practice today. More factors are involved in their application to subways. But the basic procedures for stress analysis are alike, varying only with the particular method familiar to or preferred by the individual designer. Dozens of variations in design procedures have been published, and design of rigid frames is now taught in all schools.

With reference to the design of welded rigid frame subway bents, considerable study and efforts were devoted to devising tabular methods and to machine operations, all based on moment distribution, aiming for accuracy, speed and economy. Results were most successful. After initial procedures are instituted, engineering costs for design of welded rigid frame subway bents will be no higher than those for the standard articulated riveted bent of today.

PRESENTING THE WELDED RIGID FRAME SUBWAY DESIGN

In October 1945 the *Engineering-News Record* published an article describing a subway building program aggregating one billion dollars, projected for postwar construction in New York City. Included as its main item was the Second Avenue line specially considered here.

In a letter dated Dec. 24, 1945, addressed to James H. Griffin, Chief Engineer, Board of Transportation, New York City, I presented officially to New York for a period of five years all patent rights and all my compiled research and design data until Jan. 1, 1951. This data allowed 5 yr. for taking advantage of my gift, or one year longer than the anticipated maximum of 4 yr. for completing actual construction. A most cordial acknowledgement followed immediately with quick action by the Chief Engineer, Mr. Griffin.

He held a conference with his staff of executive construction and design engineers at which I was present. There I reviewed briefly the highlights of the welded rigid frame subway bent. Mass production of identical brackets was emphasized as one of the major factors contributing to its economy. Erection was reduced to simplicity and safety, using a minimum of field joints, their location occurring only in the floor or roof beams near the points of contraflexure. The outside columns together with the roof and floor beams alone formed integral parts of the rigid frame bent. Interior columns would remain as at present, generally 6 in. wide and theoretically hinged top and bottom. Design costs for engineering work would be no higher than at present. Other items mentioned included the program of research and study that preceded my design.

That in brief was the story of the welded rigid frame subway bent, the product of almost a lifetime of effort and expense—theirs to own and to keep and exploit to the full in the public interest the vast savings in steel tonnage, excavation and millions of dollars in costs. No royalty was requested, nothing either in payment of the costs and expenses incurred in research, development and for the patent. On file in the Board of Transportation was my original letter to the late A. I. Raisman, Chief Deisgning Engineer, recording from the beginning the intent back of all my work.

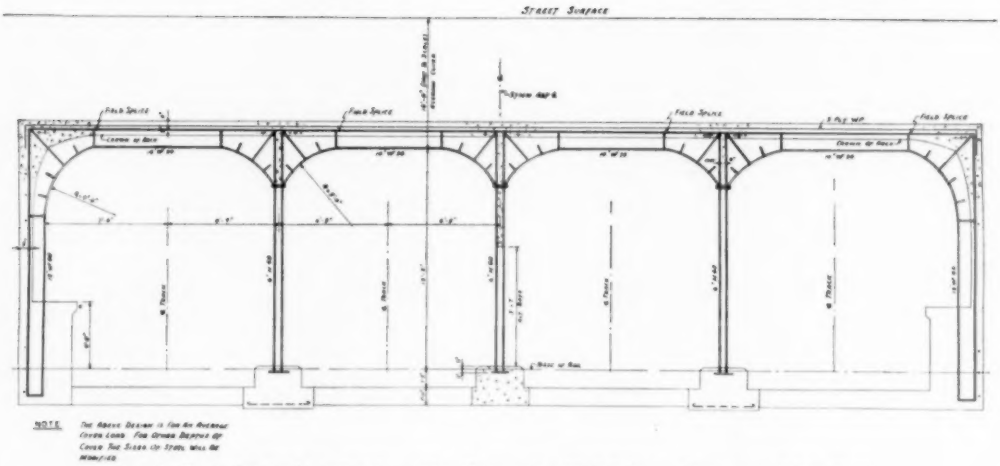

Fig. 7 Typical 4-track section in earth. Proposed welded design. Bents spaced 5 ft. C. C.
Board of Transportation of The City of New York Engineering Bureau

My presentation ended with the declaration that if impartial and independent investigation and study revealed any single item technically faulty or not as represented by me, consideration of my design could be discarded at once. Thus without qualification or reservation were bared the cold facts ready for examination.

A live interest by all present was manifested at the above conference. Their questions were sincerely motivated and their attitude was most amicable. A vote was then taken in my presence and they declared unanimously in favor of officially investigating my design. Two subway engineers with the Board of Transportation specially selected by Chief Engineer Griffin for their qualifications and ability were appointed a committee to conduct an investigation independently: H. Newman, representing the Construction Division, and N. D. Brodkin, representing the Designs Division, the committee reporting directly to the Chief Engineer.

I gave the committee considerable data and designs from my own files. Also covered verbally were various phases of design and the results revealed from my study, research and tests. I visited them at their offices occasionally to note their progress and answer any questions that might have arisen. They were introduced to Technical Secretary S. A. Greenberg at the offices of the American Welding Society, after which I left them so as not to influence their questions or inquiries.

OFFICIAL REPORT ON WELDED RIGID FRAME SUBWAY BENTS

Six months after beginning their investigation the committee of subway engineers completed their report. Chief Engineer Griffin sent me a copy.

Using a cover of 14 ft. which is considered average for the proposed subway, the committee worked up typical designs in accordance with their own judgment. The technical data which I left with them and my advance suggestions were used only for guidance and disposition as they saw fit.

Figure 7 represents a copy of their design for a typical section above water. They used round knees with intermediate stiffeners instead of angular knees without stiffeners which I preferred. For the typical section above water their design saved 15% in weight of structural steel. For the typical section with invert under water their design saved 23% in weight.

The report covered thoroughly all technical matters and questions that were raised at the original joint meeting with the Chief Engineer and his executive staff. The vital question of stresses due to possible differential in settlement was analyzed with skill and disposed of as presenting no problem affecting the rigid frame under normal conditions.

My contention that engineering costs of rigid frame design would not surpass that of the conventional design was sustained.

The matter of reliability or unreliability of field welds was eliminated with decisiveness in their faith in welding that is properly supervised. Field erection was considered as presenting no problem.

Summarizing briefly the substance of the official report, it sustained 100% technically the efficacy of the welded rigid frame subway bent, its vast savings in steel tonnage and in excavation due to the use of shallower roof and floor beams with the rigid frame designs. The investigation was conducted independently, honestly and with skill. Because of the extraordinary postwar conditions prevailing in the fabricating industry at that time, the Spring of 1946, the savings in costs resulting from the vast reduction in steel tonnage could not be rationally determined.

In acknowledging receipt of the official report I wrote the Chief Engineer taking issue on only one major point, the committee's use of round knees with intermediate stiffeners. It was not a fault but merely a professional difference of opinion. Whereas for buildings the knees in rigid frames are not of primary importance, in subway design the shape and size of the prefabricated bracket is the focal point about which economy is based.

For the benefit of those interested in this subject, a short explanation might be of interest.

I do not favor round knees for subway rigid frame bents mainly for economy. My decision is based on study of fabricating methods and on tests of knees. Brackets with round inner flanges cost more to fabricate. Some engineers representing the welding equipment manufacturers lose sight of that point when they think only in terms of their automatic cutting apparatus. Rounded web plates for brackets cannot be sheared like straight plates. On the other hand webs with straight inner flanges can be sheared or flame-cut. The fabricating companies overwhelmingly favor the square or angular knees for expediency and economy. Stress analysts who have worked with me or whom I have consulted support my selection of the square and angular knees in preference to the round knee for subway design. Although there is nothing wrong with the round knee, where mass production of prefabricated knees or brackets is required as for subway bents, haunched and square knees are definitely more economical.

Curved inner flanges in knees are preferable to almost all designers of rigid frames, particularly the beginners. It is claimed that they permit smoother stress flow. Furthermore they appear less for-

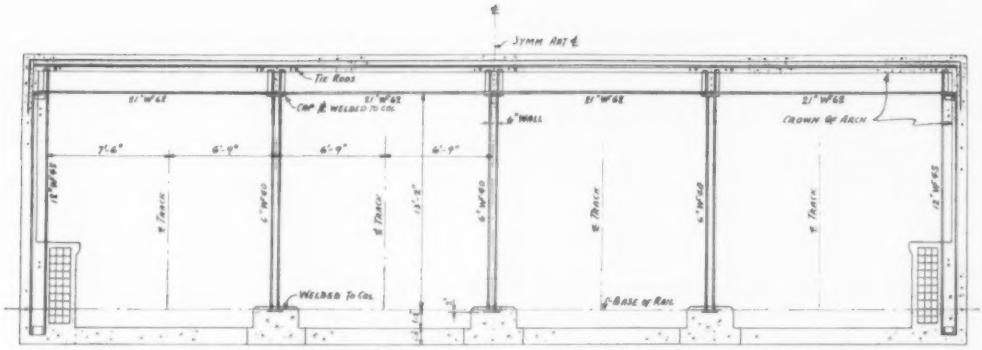

Fig. 8 Typical riveted bent in earth. Bents: 5 ft. C. C. Estimated weight, 7300 lb.

midable to analyze. Many textbooks cover the theory of curved beams, among them Timoshenko's Vol. II which to many designers is close enough for stress analysis of curved knees.

On the other hand angular knees whether haunched or square plainly present challenges to stress analysis more to be avoided than used.

From an academic viewpoint the selection of a round knee merely because it permits a smoother stress flow does not insure that that choice will afford greater strength to the rigid frame. I have found from tests that haunched knees and all-square knees possess greater strength for equivalent sizes of knees. That point was quickly recognized by Mr. Brodkin, Design representative on the subway investigating committee, after he saw my letter and critique of his design which I sent to the Chief Engineer. Several months following the official report, he made new and unofficial designs based on my prototype using a combined haunched and square knee. A copy of those designs was forwarded to me.

Figure 8 is a reproduction of a typical riveted section in earth and above water with 14 ft. cover. Figure 9 shows a welded rigid frame design for the same loading conditions, affording a 22% reduction in steel tonnage. Figure 10 is a reproduction of a typical riveted section with invert below water. Figure 11, a welded rigid frame design for the same loading conditions with invert under water shows a savings of 28% in weight of structural steel.

Note the vast savings in steel tonnage. Not mentioned here are savings in excavation in considerable quantities, especially in rock which will be encountered along the Second Avenue subway route.

Note also the simplicity of the design for fabrication, for mass production of identical knees and the simplicity in erection of the welded rigid frame subway bent; then pause to consider which you would prefer to use.

It is perhaps reasonable to assume that all connected with the welding industry would naturally select the welded rigid frame design. It is also a safe guess that some fabricators would just as naturally select the riveted design because it involves much more steel or because they prefer riveted work or for both reasons.

Since I have been using riveted design for 40 odd years down to this day, would it not have been most natural for me to select a riveted design for rigid frame subway bents? I did not do so for obvious reasons.

A riveted frame subway bent would be loaded down with splice and reinforcing plates resembling a structural monstrosity, and its cost would be higher than at present.

By comparison, the welded rigid frame subway bent would stand out in its monolithic form as natural in its shape as though produced by some special operation at the rolling mill.

Thus welding alone makes possible the vast economies, the simplicity of fabrication and erection of rigid frame subway bents.

Those are the simple facts which automatically eliminate the matter of choice.

In the face then of this comparison between welding and riveting, let us for a moment examine others' opinions concerning welded fabrication.

THE VOICE OF THE STEEL FABRICATORS

In 1943 the American Bridge Co. published extensively the following advertisement: "Structural welding has a comparatively short history but it is crammed with accomplishment . . . Nothing better illustrates the giant strides welding has made in speeding up, simplifying and reducing the weight of structural fabrication . . . "

Pause for a moment to consider carefully those statements "speeding up, simplifying and reducing the weight of structural fabrication" which take on added importance because of their authoritative source—one of the world's largest steel fabricators.

Continuing with excerpt from that same advertisement by the American Bridge

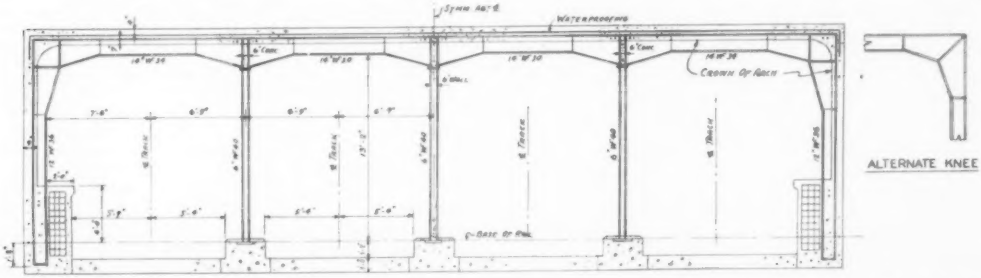

Fig. 9 Typical welded rigid frame bent in earth. Bents: 5 ft. C. C. Estimated weight, 5770 lb. (Note: Above designs are for average cover, 14 ft.)

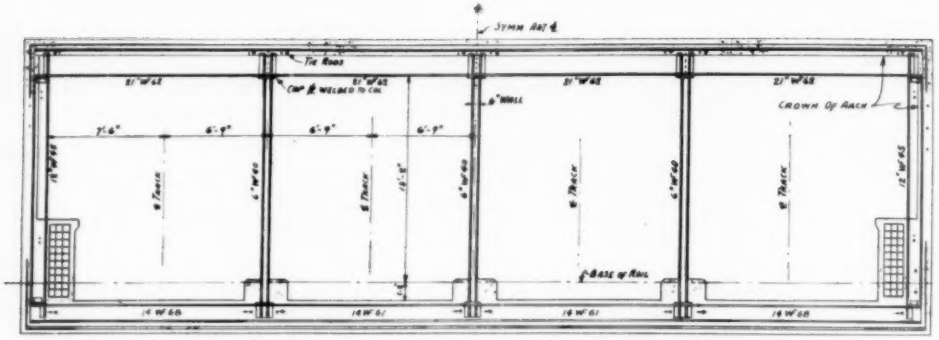

Fig. 10 Typical riveted bent-invert in water. Bents: 5 ft. C. C. Estimated weight, 11,040 lb.

Co.: "Our welding facilities have doubled and redoubled . . . Shop forces have been intensely trained to new skills . . . it is this wealth of technical knowledge and wide practical experience, supplemented by equipment and facilities second to none, that stands ready to help you in your post-war construction program. Welding will have a large part in it and we know welding . . . "

Without doubt other fabricators will claim, and rightly so as does the American Bridge Co., that they too know welding. Note, for example, the advertisements of the Ingalls Iron Works, among the largest and most progressive fabricators. Referring to the Archibishop Stepinac High School in the *Engineering-News Record* of Mar. 10, 1949, they state "Ingalls chose to weld the girders primarily to save weight . . . Steel was saved, time was saved and the finished girders presented exceptionally straight and smooth appearance . . . If you have a construction problem call on Ingalls, pioneer in modern welding and fabrication. . . "

This viewpoint is reflected even among the smaller companies as, for example, the Lehigh Structural Steel Co., one of the most efficient fabricators of welded steel. In an article headed "welding Conserves Steel," published in the *Engineering-News Record* of Oct. 28, 1948, T. R. Mullen, President of Lehigh Structural Steel Co. says: "Our experience as structural steel fabricators reveals that the available supply of structural steel can be made to go much further through the use of welding . . . Welding permits compact connections which minimize and sometimes altogether eliminate connection material. . . "

Thus the above advertisements selected at random seem to be a clarion call from the structural steel fabricating world announcing:

"Attention, Engineers, Architects, every one!
Bring on your welded work and we will show you economy.
We will show you quality based on our know-how and experience with welding.
We will show you speed—speedier than ever before, with welding! . . . "

LIGHT AND SHADOWS OVER WELDING

Rays of sunshine now beam upon welding springing from public declarations recorded in the "Voice of the Steel Fabricators." Occasionally, however, news reveals dark shadows over welding, emanating from the same source.

At a recent welding conference which I attended a speaker of national prominence declared that he had learned during travels throughout the country that many steel fabricators buck welding. Since he is not affiliated with manufacturing of welding supplies or equipment, his statement cannot be construed as having a sales motive. The truth of what he said is of course known to many persons. It is, however, so serious that we might ask what will the fabricating industry do about giving up about 23% of the steel tonnage to technological progress—to the use of welded rigid frame subway bents?

We can see how in some cases of welded buildings the structural design is so crude as to merit opposition. Furthermore, the precision required in welding for buildings, the lack of duplication of pieces and what amounts to "tailor-made" units add up to familiar arguments against welding used by some fabricators. One cannot very readily take issue with them on these points. However, none of these arguments can hold against the welded rigid frame subway bent as illustrated in Figs. 9 and 11. On the contrary its mass production of identical units, its simplicity and the magnitude of the project at once set it apart as a type of structure with advantages without precedent in structural welded fabrication. Does any one know of any comparable peacetime structure?

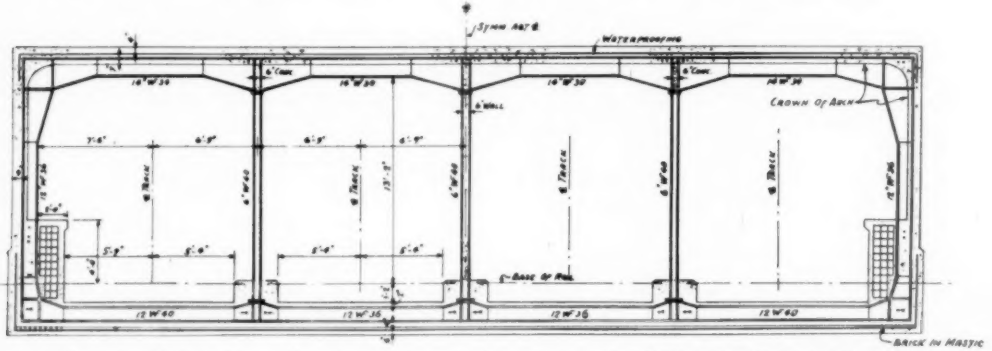

Fig. 11 Typical welded rigid frame bent-invert in water. Bents: 5 ft. C. C. Estimated weight, 7900 lb. (Note: Above designs are for average cover, 14 ft.)

It is not my intention here to take up a crusade for welding, for nothing I would say could compare with the factual record in the "Voice of the Fabricators" just heard. I would, however, subscribe unremittently to a crusade for truth and honesty about welding.

To this end I shall delve just a bit into my confidential file with regard to the welded rigid frame subway investigation.

One of the largest fabricators told me that when the bidding time came, he would sharpen his pencil and "go places" if I used haunched or square knees (they are the only type I would use). A shipbuilding company official proclaimed its vast savings in a review of the design. The official of another shipbuilding company looked forward eagerly for an opportunity to bid on this work which he believed could be produced for a new record output by his organization.

The owner of a fabricating and welding shop declared this type of work a "dream of a production job that comes only once in a lifetime."

I can go on with a list of other favorable comments and letters, supporting the welded rigid frame subway bent design in tune with the "Voice of the Steel Fabricators." Let us, however, turn for a moment away from the sunny side to observe a huge shadow cast over welding. In a statement comparing the costs of the welded rigid frame subway bent with the standard articulated riveted bent, a large fabricator declared that the welded rigid frame would cost almost three times as much as the riveted bent. Does any one here believe that the welded rigid frame bent in Fig. 7 or in Fig. 9 would cost 2½ to 3 times as much as the riveted bent in Fig. 8? That statement, stripped from its diplomatic integument, would read "We want much more steel and no welding."

It is my belief that if the above fabricator were familiar with the new possibilities for new markets afforded by execution of the welded rigid frame subway project, he would get in tune to lead with the command "Let's Go!" for rigid frame subways.

LET'S GO!

Los Angeles, San Francisco, Washington and other cities both here and abroad are conducting studies for new subways. Toronto, Philadelphia and Chicago have new subways in process of construction. Subways remain a dream toward which all large cities aspire for the optimum in rapid transit.

Most of those cities begin their plans with all-concrete construction, and you will observe that such construction is always massive, slow in completion and its form somehow a reminder of construction of ancient times. Steel fabricators are precluded right from the start from ever reaching forth into such markets.

Why is concrete construction selected? Mainly for economy.

Can the steel industry get a look into it? Yes, with welded rigid frame construction and only that type, packing the greatest strength in the least space in the speediest construction time.

How could the steel industry get started? By leading the way with rigid frame subway construction right now in New York City—the best opportunity for them to show what Steel can do with mass production.

How about the 23 to 25% reduction in steel tonnage which they would sacrifice to New York City's benefit?

That very savings would boomerang back in their hands opening up new markets never before considered available! The greater the savings in steel, the more public interest would they invite. And it is just this difference, plus the savings in excavation, plus the factor of speedy construction possible only with structural steel, that would ring the bell for structural steel subways where concrete construction alone has been used, for subways in cities that heretofore could not afford them.

The late Robert Ridgway, Chief Engineer of New York Subways, once told me that officials from principal cities everywhere who were interested in rapid transit had come to visit him and study the New York subway system. The new markets thus will not only become national but international. Subways now in South America were built under American engineering direction.

This briefly is the business analysis outlining the possibilities for newer and greater markets against the immediate reduction in steel requirements. If this makes horsesense and you now feel alerted to sharpening your pencil to a new approach, let us concentrate on a thumb-nail sketch and recapitulation of the welded rigid frame subway bent from a fabricator's production standpoint:

(*a*) Fabrication involves mass production of identical knees welded to the sidewall columns and to the roof and floor beams.

(*b*) Jigs of only one size are required for the outside corner brackets and of a single size for the interior brackets where there are no floor beams.

(*c*) For frames with floor beams where the invert is under water, there again a single size jig is required for the corner brackets and one for the interior brackets in the floor.

(*d*) Field welding is confined only to locations in the roof or floor beams near points of contraflexure. In some cases, as for example the site shown in Fig. 4, the rigid frame can be field welded flat on site thus making for instant and most economical erection.

(*e*) Each bent is a simple complete structural unit and is itself free and independent of its adjacent bent except at stations. Thus there is no problem of erection tolerances or clearances in this work that is encountered in welded tier buildings. Only the subway stations would be comparable in that respect and as one-story structures they would present no problem new to welded structural design or to fabricators.

That completes the picture in compendium. Viewing it as a cold business proposition offering new markets and opportunities and new sources of profit to the entire steel and welding industries, profits to every one except to me, am I too optimistic to believe that the command, "Let's go with welded rigid frame subway bents!" will now be boomed without a single exception to the tune of the above recording of the "Voice of the Steel Fabricators?"

It is my firm belief and hope that the steel fabricators will bend forward with new efforts geared to modern semi-automatic welding equipment, dig in and set up new records in the manner of the war production period in keeping with the traditional American spirit of progress, producing more and better products at lesser costs for more consumers, building more subways for more cities at lesser costs via steel—welded steel rigid frames!

FAITH

More than 30 yr. ago when the late Clifford Holland was still engaged in research for the design and construction of his vehicular tunnel under the Hudson River, I learned from him some of the pains of pioneering. No matter what conclusions were reached after long study by him and his associates, there were always other engineers who differed widely or even declared that his tunnel design could not be built. The difference between Holland and the dissenters was the usual difference between pioneers—those who do things and those who talk them. Some of the incidents told me in private were published in later years in the *ASCE. Proceedings.*

The welded rigid frame subway bent did not come forth in its present state of development as a technically efficient and sound design without its own brand of labor pains. The statement "It cannot be done" was bounced along its path of progress with minor hurdles and criticisms all of which today are like so much water over the dam. Perhaps my long service and battle experience as an Infantryman served to steel me against the many obstacles of that nature and prepare me to pursue the objective to victory, do or die. And I am reminded of the cryptic words of my old army mentor, the late Gen. George H. Patton, "If it can't be done, let's do it!"

If there is any engineer of mature age who has gone through life without designing structures that "could not be designed," without building structures that "could not be built," he has missed what I consider among the greatest thrills in professional engineering.

It was obviously in anticipation of every variety of obstacle and criticism that so much time and energy were spent to make the investigation and study of the welded rigid frame subway bent as thorough as possible. When the time arrived for its presentation to the City of New York, I felt prepared.

At this time, almost 40 yr. after I started construction of the first concrete rigid frame subway shown in Fig. 1, its grandchild, the welded rigid frame subway bent, emerges in life's twilight as a grand adventure—its prize and compensation realized in the official recognition that it can be done, that it is safe and economical.

Neither can anything be so gratifying and encouraging as the acclaim accorded this design by so many fellow engineers who have reviewed it, a few of whom may be in this audience today.

If now in the name of this national audience of the AMERICAN WELDING SOCIETY it were possible for me to donate to New York City the cash sum of $10,000,000 toward their new subway costs even as New York pursued the conventional design of riveted construction, that sort of gift would not be so valuable to the future of subway construction, to science, to industry and particularly to every one present as would this opportunity of using welded rigid frames.

In line with my original plan beginning in 1925 and reaffirmed later in 1937 and 1938, I will as soon as I am commissioned Consultant on this project consider all expense incurred throughout the entire development of this design, its research, its patent, its royalty, a gift to technical progress. I will act similarly toward other municipalities, toward steel fabricators and contractors planning to pioneer welded rigid frame subway construction.

The great adventure is now behind me with its years of work and trials and costs; its prize is before you technically complete and officially approved, yours to exploit to the full for its accomplishments, its profits and new markets that await you. The greater your accord in its reception, the greater will be your reward. It is hoped that it will command your individual as well as your united interest and wholehearted support.

ACKNOWLEDGMENTS

Thanks are hereby extended to the following for their outstanding work and assistance:

To William H. Wolf of Denver, Colo., a former Associate, now Project Engineer with the Bureau of Reclamation, for his original work on designs.

To Arthur E. Clark, Senior Civil Engineer in charge of construction of New York subways in Manhattan and Bronx, New York City, for his guidance and advice on subway construction.

To Albert Goertz, Designing Engineer, Retired, New York Subways (who designed the concrete rigid frame subway which I helped build in 1910) for his guidance and advice on subway design.

To the Lincoln Electric Co., Cleveland, Ohio, and the various technical members of their staff who contributed so generously their assistance and guidance on welding.

To LaMotte Grover, Welding Engineer, Air Reduction Sales Co., New York City, for his assistance on welding and design details and his constructive criticism.

To the many individuals and companies contacted down through the years whose cooperation made possible the attainment of accurate information and factual data.

Welding of Stainless Clad Steel

Problems encountered in the field erection of thin and Heavy Gage stainless clad steels and their solutions

by Perry C. Arnold

INTRODUCTION

EACH fabricator of welded vessels is required, by various codes and specifications, to properly qualify the welding procedure that he intends to use on a structure. In so doing, the manufacturer encounters numerous problems that have to be solved before the physical requirements of the qualification procedure can be fulfilled.

Many have found that the problems associated with the welding of stainless clad steel are more numerous and much harder to solve than those encountered in the welding of mild or slightly hardenable steel. Not only must the physical requirements of the tensile and bend tests be met, but also certain corrosion requirements.

The primary purpose of this paper is to describe a few of the problems that have been encountered in the welding of field constructed stainless clad vessels. Your attention is called to the fact that the problems to be described concern only field erected vessels. The field has many difficulties to overcome that are seldom encountered in the shop. Some of these are: weather conditions, welds made in all positions, shaking scaffolds and quite regularly the welding must be performed on a structure high in the air.

A secondary purpose of this paper is to encourage others who have encountered similar difficulties to describe their particular problems and solutions. It is believed that many welding engineers have discovered val-

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Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

uable facts concerning the welding of stainless clad steels but hesitate to discuss them thinking that they are of minor importance. The final solution of most welding problems comes from the development and use of many seemingly trivial details. Progress is always rapid when ideas are exchanged freely. There are many willing to help solve the problems when the problems are made known.

WELDING OF RELATIVELY THIN STAINLESS CLAD MATERIAL

Many field fabricated storage vessels for corrosive products are made of $^3/_{16}$ and $^1/_4$-in. clad material. The stainless portion varies from 10 to 20% of the thickness of the material. This thickness requires different joint details and welding procedures than heavier material. Figure 1 shows two joint designs that are quite commonly used. Several different welding procedures may be used on each type.

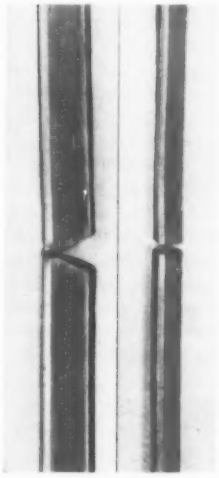

Fig. 1 Stainless clad joint details

A standard procedure is to first weld the mild steel side with mild steel electrodes, then to back-chip or grind the stainless side to clean weld metal and, finally, to weld the stainless steel side with stainless steel electrodes. When this procedure is followed, there always is danger of the mild steel deposit penetrating into the cladding and picking up enough chromium or nickel to make a very brittle hard weld. There is also the problem of mild steel weld particles blowing through the joint and becoming imbedded in the stainless steel surface. These particles if not removed will give corrosion trouble under certain service conditions. These particles can be discovered by washing the cladding with either dilute nitric acid or a saturated solution of copper sulphate.

A simple solution to both of the above problems is to weld both sides of the joint with stainless steel electrodes. The additional cost in using stainless steel electrodes is largely offset by a saving of time in back-chipping or grinding of the joint and cleaning of the clad surface. The mild steel side of the joint should always be welded first unless the welding position requires that the stainless steel side be welded first in order to provide down-hand back-chipping or grinding. Also, many have learned that depositing a bead on the mild steel side after the stainless steel weld has been completed facilitates the cleaning of the slag from the stainless steel weld beads.

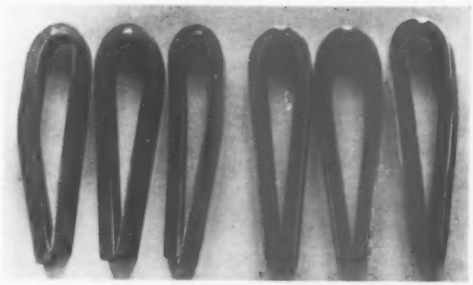

Fig. 2 Stainless clad free bends

Figure 2 shows six free-bend specimens taken from a test plate welded with mild steel and stainless steel electrodes. The cladding was of the 18 Cr–8 Ni type and welded with 25 Cr–12 Ni electrodes (lime coated). The mild steel portion was welded with E6010 electrodes. Three specimens were bent with the mild steel (M. S.) surface in tension. The other three were bent with the stainless steel (S. S.) surface in tension. The elongations secured in the as-welded condition were:

Specimen No.	*M. S. in tension, %*	*S. S. in tension, %*
M-1	52	..
M-2	36	..
M-3	36	..
M-4	..	44
M-5	..	36
M-6	..	36

Figure 3 shows six free bend specimens taken from a test plate where both the mild steel and stainless steel sides were welded with stainless steel electrodes. Again the cladding was of the 18 Cr–8 Ni type and both sides were welded with 25 Cr–12 Ni electrodes. In the as-welded condition the elongations secured were:

Specimen No.	*M. S. in tension, %*	*S. S. in tension, %*
S-1	32	..
S-2	35	..
S-3	30	..
S-4	..	28
S-5	..	28
S-6	..	29

Fig. 3 Stainless clad free bends

WELDING OF STAINLESS CLAD MATERIAL OF 3/4 IN. THICKNESS OR MORE

The welding of heavy clad material presents many problems to the welding engineer. This is especially true when the welds have to be made in the field and in all positions. The heavy material provides high restraint and fast quenching of the weld beads which often produce cracks and low ductility in the weld metal and fusion zones. We have found that it is much more difficult to qualify the welding procedure for the horizontal welding position than for any other position. Due to this fact, the following discussion and data will refer to experimental test plates that were welded in the horizontal position. All plates welded and tested were large enough to provide restraint and fast quenching of the weld metal. Field welding conditions were approximated as closely as possible.

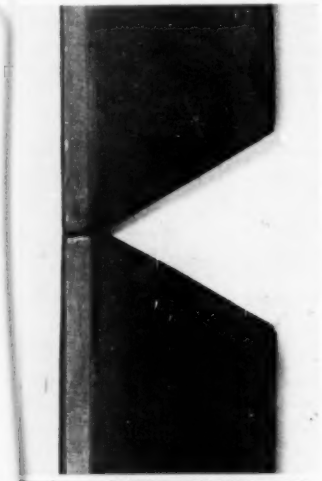

Fig. 4 Heavy plate stainless clad joint details

Figure 4 shows a joint detail that has been recommended by many for the welding of heavy stainless clad material. This detail requires most of the weld metal to be deposited from one side. It also requires the depositing of the least amount of stainless steel weld metal. It also is one of the easiest joint details to qualify in accordance with the code requirements. Unfortunately, many purchasers object to its use because of two very good reasons.

First, since the major portion of the weld metal must be deposited from one side of the joint, the plate tends to distort and buckle badly. Figure 5 shows the plate in Fig. 4 after all welding has been completed. Note the tendency of the plate to warp toward the mild steel side of the material. Secondly, note that the depth of the stainless steel weld metal is over twice the thickness of the cladding. Many purchasers object to having this amount of cross-sectional area filled with stainless steel weld metal, especially if the clad material must work at relatively high temperatures.

The above objections are overcome by another joint design as shown in Fig. 6. The mild steel portion of the material can be welded from both sides which overcomes the distortion problem. The thickness of the stainless steel weld metal can also be held to a minimum. Figure 7 shows the plate in Fig. 6 after all welding has been performed. Note that the distortion is held to a minimum as is likewise the thickness of the stainless steel weld metal. However, much more stainless steel weld metal is required than in the single-vee groove weld because of the greater width of the face of the stainless steel weld. We have also found this joint far more difficult to qualify as will be shown later. Nevertheless, many prefer this type of joint and it is rapidly approaching standardization.

Figure 8 shows a number of free bend specimens taken from the single-vee groove weld. All specimens were bent with the clad portion in tension. The backing is 1-in. thick A.S.T.M. A201 Grade A material. The cladding is approximately 1/8-in. thick, Type 410 material held to a maximum of 0.08 carbon. The mild steel portion was welded in the horizontal position with

Fig. 5 Test plate as shown in Fig. 4 after welding. Note excessive warpage

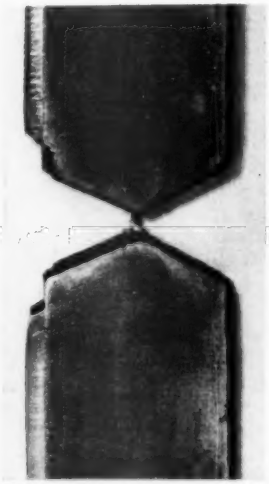

Fig. 6 Heavy plate stainless clad joint details

Fig. 7 Test plate as shown in Fig. 6 after welding. Note warpage is held to a minimum

Fig. 8 Free-bend specimens from single-vee type groove

E6010 electrodes. The cladding was also welded in the horizontal position with 25 Cr–12 Ni electrodes (lime coated). The plate was allowed to cool to room temperature (70° F.) after each bead was deposited. The specimens were bent in the as-welded condition. The stainless steel weld metal gave the following elongations:

Specimen No.	*Initial gage length, in.*	*Final gage length, in.*	*Difference, in.*	*Elongation, %*
H-1	0.40	0.49	0.09	22.5
H-2	0.39	0.45	0.06	15.4
H-3	0.40	0.52	0.12	30.0
H-4	0.39	0.49	0.10	25.6
H-5	0.40	0.49	0.09	22.5
H-6	0.40	0.49	0.09	22.5

Note that all of the free bend specimens, with the exception of H-2, elongated the required 20%. However, as stated before, the excessive warpage resulting from this procedure plus the greater depth of stainless steel in the mild steel portion of the joint, prohibits its use on many jobs.

Figure 9 shows a number of free bend specimens taken from a double-vee groove weld. All specimens were bent with the clad portion in tension. The clad material is the same as described for the single-vee joint. E6010 electrodes were used for the mild steel weld metal and 25 Cr–12 Ni electrodes were used on the clad side. All beads were deposited in the horizontal position and allowed to cool to room temperature between passes. All specimens were bent in the as-welded condition. All of the specimens were machined as per code requirements. The clad surface was hand polished to remove the effects of work hardening.

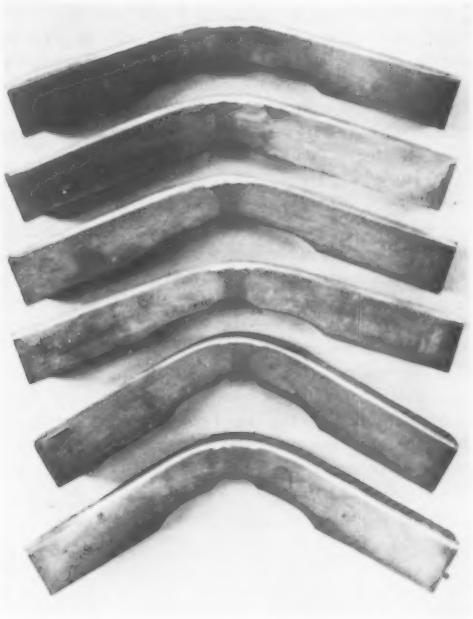

Fig. 9 Free-bend specimens from double-vee type groove

The stainless steel weld metal gave the following elongations:

Specimen No.	*Initial gage length, in.*	*Final gage length, in.*	*Difference, in.*	*Elongation, %*
H-1-D	0.80	0.93	0.13	16.2
H-2-D	0.81	0.91	0.10	12.3
H-3-D	0.81	0.90	0.09	11.1
H-4-D	0.81	0.98	0.17	21.0
H-5-D	0.81	0.93	0.12	14.8
H-6-D	0.81	0.96	0.15	18.5
H-7-D	0.81	0.98	0.17	21.0

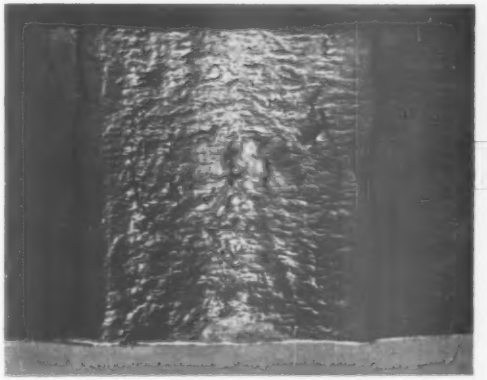

Fig. 10 Free-bend specimen after elongating 21%

It is apparent from the above elongations that the required 20% elongation is not easily or consistently secured. Figure 10 is a face view of a bend specimen that elongated 21%. Note the minute failures in the stainless steel weld metal. Also note that the weld metal tends to elongate primarily in the refined and annealed zones produced by one bead overlapping another.

In our search for a welding procedure and electrodes that would give us consistently acceptable results, many experimental plates were welded and tested. Numerous combinations of electrodes were used such as E6010, E6013 and low-hydrogen electrodes for the mild steel side, and 25–12, 25–12 Cb, 25–20, 25–12 Cb plus 18–8, etc., for the clad side. Stress-relieving of the specimens was also tried to see if it might not be easier to secure 30% elongation on stress-relieved specimens than 20% on nonstress-relieved specimens. Metallurgical specimens were made of the failed stainless steel weld metal in hopes of learning the cause of the failure. Each of these experiments contributed its part to a solution of the problem.

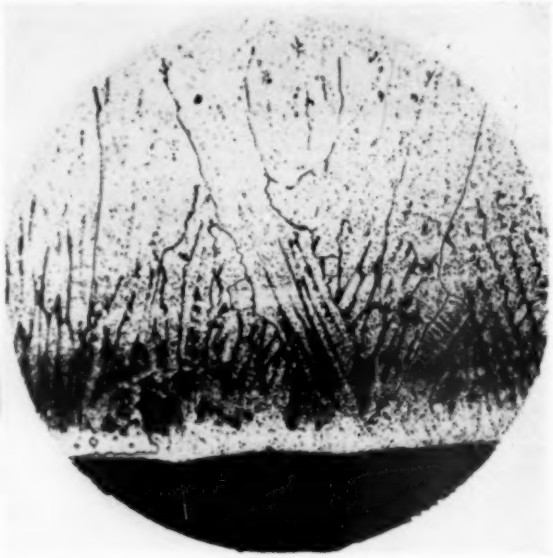

Fig. 11 Photomicrograph of junction of stainless steel weld metal and mild steel weld metal (at bottom). 200 ×

Figure 11 is a photomicrograph of the junction of 25–12 weld metal with mild steel weld metal. Considerable diffusion of carbon into the weld metal along the columnar grain boundaries can be noted. Other metallurgical specimens revealed many minute cracks on the grain boundaries of the columnar structure of the 25–12 weld metal. We reasoned that if carbides were precipitating on the grain boundaries and contributing to failures, then 25–12 electrodes stabilized with columbium should tie up the carbides and prevent failures.

Figure 12 shows three free bend specimens taken from plates that were welded with 25–12 Cb electrodes. Note that the appearance of the stainless weld metal is good. However, the elongations secured on these three specimens were only 17, 18 and 19%. We had succeeded in improving the appearance of the stainless weld metal, but we had failed to secure the required 20% elongation.

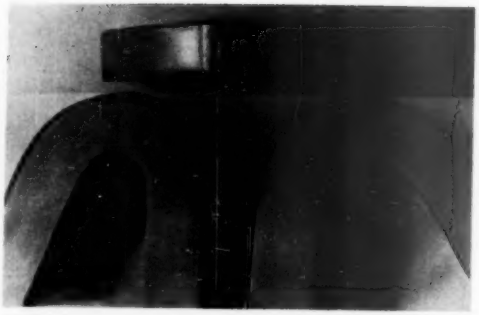

Fig. 12 Stainless clad free-bend specimens. Clad portion welded with 25 Cr–12 Ni-Cb electrodes

We next tried stress-relieving several specimens that were welded with the 25–12 Cb electrodes. Again the specimens after bending showed sound stainless weld metal free of the small fractures, but short of the required 30% elongation by 2 or 3%.

Close inspection of the bend specimens indicated that the cladding was elongating more than the stainless weld metal. This meant that the weld metal was too strong for the cladding. Tensile specimens also substantiated this point. Did this mean that the weld metal should be made weaker in order to secure more elongation through the weld? The Chairman of the A.S.M.E. Boiler Code Special Committee on Clad Vessels was consulted on this point. His reply was to the effect that he was aware of this problem but thought that the code elongation requirements should be met. At this point may we call to your attention the fact that the A.S.M.E. Code Case 896 has since been revised to allow reduction of the base metal thickness below the stainless weld on transverse specimens and to allow the making of longitudinal free bend specimens. These new procedures force the weld metal to yield as much as the surrrounding cladding.

However, before Code Case 896 was revised and adopted, we succeeded in discovering a welding procedure that would give satisfactory transverse free bends. Our first thought was to make the weld metal as nearly similar to the cladding as possible. This meant the use of straight chromium electrodes. In order to use these electrodes, the clad material would have to be preheated, postheated and stress-relieved. This procedure would often prove both impracticable and uneconomical on many field jobs, so it was discarded and other procedures tried.

Fig. 13 Stainless clad free-bend specimens. Clad portion welded with 25 Cr–12 Ni-Cb electrodes (1st layer) and 19 Cr–9 Ni electrodes (2nd layer)

Since the 25 Cr–12 Ni stabilized weld metal had performed so well in tests, we continued to use 25–12 Cb electrodes in combination with others. Figure 13 shows several transverse bend specimens taken from 1¹/₈-in. thick, Type 410 clad plates that were welded in the horizontal position. The mild steel portion was welded with E6013 electrodes. The clad portion was welded by depositing two layers of stainless weld metal. The first layer was made with 25 Cr–12 Ni columbium stabilized electrodes and the second or outer layer was made with 19 Cr–9 Ni electrodes. Each layer consisted of several string beads each of which overlapped the preceding bead about half the width of a bead and, in turn, was similarly overlapped by the succeeding bead. Each bead was deposited only after the plate had cooled to room temperature. Careful attention was paid to the welding procedure so that it would approximate field conditions. The elongations secured were satisfactory as evidenced by the following data:

Specimen No.	*Initial gage length, in.*	*Final gage length, in.*	*Difference, in.*	*Elongation, %*	*Remarks*
A	1	1.30	0.30	30	Stress relieved
B	1	1.35	0.35	35	Stress relieved
C	1	1.30	0.30	30	Stress relieved
D	1	1.31	0.31	31	Stress relieved
E	1	1.22	0.22	22	Not stress relieved
F	1	1.20	0.20	20	Not stress relieved
G	1	1.23	0.23	23	Not stress relieved
H	1	1.24	0.24	24	Not stress relieved

The above procedure is very costly; to our knowledge it never has been used on a vessel, and unless extremely unlikely service conditions arise which warrant a trial it probably never will be so used since many vessels welded on the clad side with 25–12 or 25–20 electrodes have performed very well.

In conclusion, may the author emphasize the fact that this paper is based on an effort to develop, by thought and experiment, procedures for welding clad vessels in the field, under field conditions, which will insure good service performance. It is hoped that this paper will bring to the code committees, welding engineers and electrode manufacturers a realization that the results of laboratory tests are inapplicable to field welding of clad vessels unless the experiments are made on large and thick plates which restrain and quench the welds just as actual vessels do.

Again the author invites others to write papers and freely discuss their problems and experiments in welding stainless clad materials. At the present time there is need for additional data, which may be known but not widely available, concerning the welding of Type 405 cladding which is supposed to be nonair-hardening. On numerous occasions, under field conditions, it has proved to be air-hardening and low in ductility.

WELDING METALLURGY

The internationally known book on the above subject originally written by Henry & Claussen has been completely revised, enlarged and brought up to date by G. E. Linnert. A book of 505 pages, handsomely bound, price $2.50. Order your copy now! American Welding Society.

Circuit Analysis of Frequency-Changer Welders

Analysis of the principal electrical characteristics of the main power circuits of frequency-changer welders. Electrical advantages are indicated

by W. K. Boice

INTRODUCTION

THE growing use of frequency-changer welders has stimulated much interest in their design and operating characteristics. This paper presents the results of an analysis of the principal electrical characteristics of the main power circuits of such welders.

ADVANTAGES OF FREQUENCY-CHANGER WELDERS

A frequency-changer welder has a higher initial cost than a comparable conventional single-phase welder, but in many cases offers definite advantages which more than compensate for this additional cost.

The advantages offered may be classed as welding advantages and electrical input advantages.

Discussion of the welding advantages is beyond the scope of this paper. These advantages, and alternative methods of obtaining them, have been discussed in previous literature.[1-4]

The electrical advantages accrue from the reduced kva. demand, improved power factor and (usually) the 3-phase character of the input as compared with the load drawn by a conventional single-phase welder. These result in less line current and, for any given supply system, less voltage drop. Hence the user may reduce the cost of equipment required to provide an adequate power supply or may be able to use an existing supply which could not accept conventional welders for a proposed welding job.

SCOPE OF PAPER

This paper does not discuss economic considerations involved in the choice between conventional welders with a supply adequate for them and frequency-changer welders with a less costly supply because these considerations have been outlined previously.[5]

Furthermore, comparison of frequency-changer welders with other types which also have relatively low kva. demand will not be made in this paper.[6]

The paper does discuss comparison of frequency-changer welders with 60-cycle conventional single-phase welders.

The information presented describes the electrical characteristics of frequency-changer welders in sufficient detail to help the welder user to decide whether or not such a welder should be used for a particular job, if he has data on other alternatives. It will permit the welder manufacturer to accurately predict the input to a frequency-changer welder. This will help the user in proper selection of power-supply equipment from the standpoints of voltage drop, heating of conductors and proper settings for short-circuit protective equipment.

In addition, formulas presented will permit designs of welders, and welder controls, which will make effective use of materials and avoid troubles from certain possible forms of circuit behavior, such as arc-throughs and sparking at electrode tips.

The information considered of widest general interest is given in the main body of the paper, while additional analysis, of primary interest to welding-machine manufacturers, is included in appendixes.

The analysis presented applies specifically to the most widely used frequency-changer circuit, which employs a 3-phase input.

It is hoped that the paper will contribute to a better general understanding of the characteristics of such machines.

PRINCIPAL CONCLUSIONS

1. Harmonics in the line current (components at frequencies higher than the line frequency) have no important effect on the rms. line voltage drop, in most cases. Consequently, the voltage drop may be cal-

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Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

culated on the basis that the load is a 3-phase sine-wave load, equal to the vector kva. at the vector power factor.

2. The reactance between primaries of the welder transformer has an important influence on electrical characteristics. Decreasing this reactance improves the input power factor, and reduces the no-load secondary voltage required to deliver a specified welding current.

3. Ignitron tubes used for frequency-changer welders are subjected to higher inverse voltages than tubes for conventional single-phase welders for the same supply voltage.

4. Simple formulas are now available for predicting the electrical performance of frequency changer welders, on the basis of design data.

CIRCUIT ANALYZED

The welder power circuit analyzed is shown by Fig. 1. It consists of six electronic tubes and a welding transformer with one secondary and three primary windings. All windings are on the same core. A pair of tubes, connected in parallel with each other, is connected in series with each primary winding.

Each primary winding, in series with its pair of tubes, is connected line-to-line across a different phase of the 3-wire, 3-phase supply.

The primaries are connected so that each terminal having a given polarity with respect to the secondary is connected to a different line.

The tubes of a pair, for each primary winding, are connected to pass current in opposite directions.

SEQUENCE OF OPERATION

The tubes are controlled in 2 groups of 3 tubes each. Each group contains one tube of each pair. In Fig. 1, one group (tubes A, B, C) consists of the upper tube of each pair. The remaining tubes (A', B', C') constitute

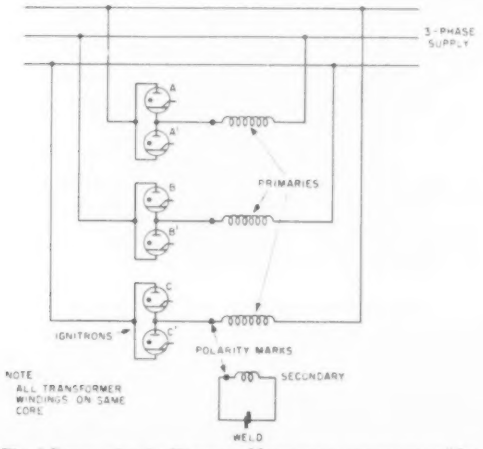

Fig. 1 Power-circuit diagram of frequency-converter welder

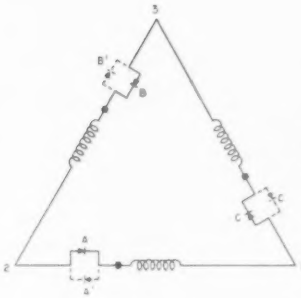

Fig. 2 Primary-circuit diagram of frequency - converter welder

the other group. Every tube in a group is connected in a direction corresponding to one polarity of induced secondary voltage. The tubes of the other group are connected to correspond to the opposite polarity of secondary voltage.

Figure 2 shows the primary connections schematically with the tubes of one group represented by dashed lines and of the other group by solid lines.

The control circuit first blocks one group of tubes from operating. The tubes of the other group are permitted to operate, and each carries current when its anode-to-cathode voltage is in the proper direction. As a result, the tubes of this group take turns carrying current. Current commutates from tube to tube, so that current is transferred to the primary winding having the highest applied voltage of the proper polarity. The current in each winding induces a voltage in the secondary of the same polarity. Thus current builds up in the secondary, and is maintained in the same direction for several supply frequency cycles. Then the starting of all tubes is blocked, and the current allowed to decay to zero. Then the other group of tubes is permitted to operate, and current is built up in the secondary in the opposite direction.

Thus repeated pulses of current in alternate directions flow through the secondary. Consequently the secondary carries single-phase alternating current. The frequency of alternation is considerably less than the frequency of the 3-phase power supply.

SECONDARY NO-LOAD VOLTAGE

The wave shape of the no-load secondary voltage is shown by Fig. 3. Figure 3 (A) shows this voltage when there is no delay (cool time) between secondary half-cycles.

Figure 3 (B) shows the effect, on no-load voltage, of retarding the starting of each heat time, thus introducing a cool time. Also, in the case shown, the first tube for each polarity is delayed slightly (30°) until the normal starting angle for succeeding tubes is attained.

Figure 3 (C) is similar to Fig. 3 (B) except that the heat time is essentially the same as for Fig. 3 (A), so that the secondary frequency is reduced by increase in the total time for one cycle.

Figure 3 (D) shows the effect of supply frequency phase control, retarding the firing of each tube beyond

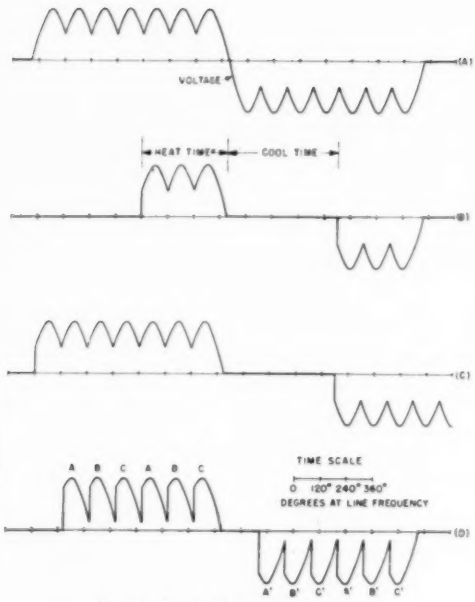

Fig. 3 No-load secondary voltage

its normal starting time. Also in the case shown, starting at each polarity is delayed about $^1/_2$ cycle (supply frequency) so that conduction always begins with Phase A.

The average value of no-load secondary voltage during one heat time is approximately[7]

$$E_{do} = \left(\frac{3\sqrt{3}}{\pi\sqrt{2}}\right)\frac{E\cos a}{N} = \frac{1.17\,E\cos a}{N} \qquad (1)$$

where*

E_{do} = average secondary voltage (no-load).
E = rms. primary voltage (line-to-line).
N = turns of one primary/turns of secondary.
a = angle of phase retard beyond normal full heat firing angle.

When firing occurs at the point 30° after the beginning of each primary wave, the angle of retard is zero, and $\cos a = 1.0$.

Since the wave approaches a rectangular form, the rms. secondary no-load voltage is practically the same as this average voltage.

In practice, open-circuit secondary voltages may not be obtainable, because there is insufficient transformer input current to cause tube ignition when the welder electrodes are open circuited. However, the calculated no-load voltage is a convenient value for use in predicting welder performance.

SECONDARY VOLTAGE REGULATION

When current flows in the transformer windings, the wave shape of the secondary voltage is altered somewhat, and its average value is reduced below the no-load value. This effect is similar to that of a conventional 3-phase rectifier, and results from the fact that current cannot commutate instantly from one primary winding to another.

The effect on the voltage induced in the secondary is shown by Fig. 4 (*A*) for the case of no phase control and constant secondary current. The dotted lines show the secondary voltage sine waves corresponding to the primary line voltages. From Point 1 to Point 2 only Primary A is carrying current. Since transformer magnetizing current is practically negligible, this primary current equals the secondary current, divided by the turn ratio. The voltage of Phase A is applied to Primary A, and this same voltage is induced in the other two primaries (if the resistance drop and tube drop in Primary A is negligible, and the secondary current is not changing).

At Point 2, the voltage of Phase B begins to exceed that of Phase A; consequently the voltage of Phase B also begins to exceed the induced voltage in Primary B. This causes positive voltage to appear between anode and cathode of one of the tubes in series with Primary B, and this tube begins to conduct.

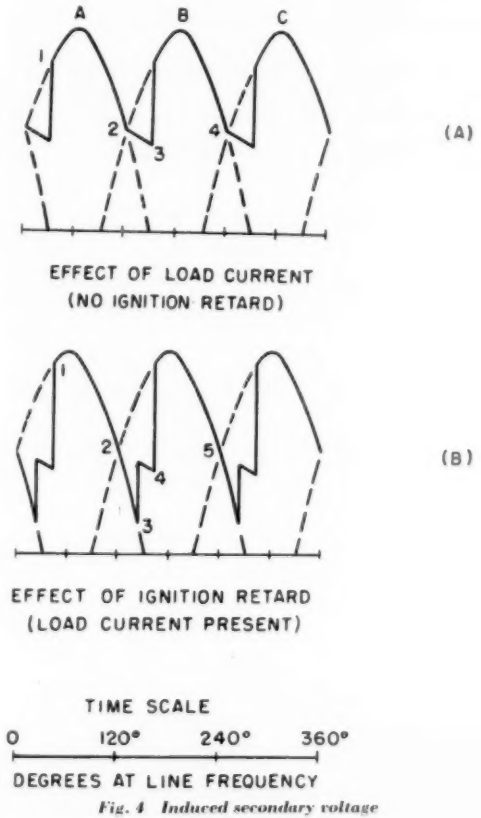

Fig. 4 Induced secondary voltage

* Complete nomenclature is given in Appendix D.

For a short time after this tube begins to conduct, 2 tubes are conducting, since the current in the tube for Primary A cannot go out instantly. During the time that both tubes (A and B) are conducting, the voltages applied to primaries A and B are equal to the corresponding line voltages, less the tube drop. After Point 2 these two applied voltages become different in magnitude, so that the voltage across Primary B becomes larger than that across Primary A. The difference in voltage is the difference in magnitude of the two line voltages

Current therefore builds up in Primary B and diminishes in Primary A, and the rate of change is determined principally by the leakage inductance between these two primaries, and the difference of applied voltages.

When the current in Primary A reaches zero, conduction stops in this winding because the tube for current in the reverse direction is blocked from operation.

At this time (Point 3, Fig. 4 (*A*)) the current in primary B has built up to the value corresponding to the secondary current. Thereafter, this current is carried by Primary B until Point 4 is reached, when commutation to Primary C begins.

During the period 2–3 the voltage induced in the secondary is about midway between the values corresponding to the voltages of the two conducting phases. Therefore, as shown by Fig. 4 (*A*), the output voltage during the period 2–3 is less than for the case of no secondary current. Thus the average output voltage is reduced, in an amount depending on the time 2–3.

The time 2–3 is increased by increasing the leakage inductance or by increasing the secondary current, because both of these factors increase the time required for the available voltage difference to reduce the current of Primary A to zero.

When ignition is retarded as by heat control, commutation does not begin until after Point 2 is reached. This is illustrated by Fig. 4 (*B*).

The average secondary voltage is approximately

$$E_d = E_{do} - \left(\frac{3X_{pp}}{4\pi N^2}\right) I_s \qquad (2)$$

where

E_d = average secondary voltage.
E_{do} = average no-load secondary voltage (see formula 1).
X_{pp} = average reactance between primaries (at supply frequency).
I_s = secondary current.

It may be observed that this formula is exactly equal to the corresponding expression[7] for 3-phase half-wave rectifiers of line-to-neutral voltage equal to the actual line-to-line voltage, and commutating reactance per phase equal to half of X_{pp}.

The reactance between any two primaries may be measured by shorting one primary winding and applying a.-c. voltage to the other, when the secondary and the third primary are open-circuited.

The effect of transformer resistance and of the leakage inductance between primary and secondary may be considered by adding these values to the secondary

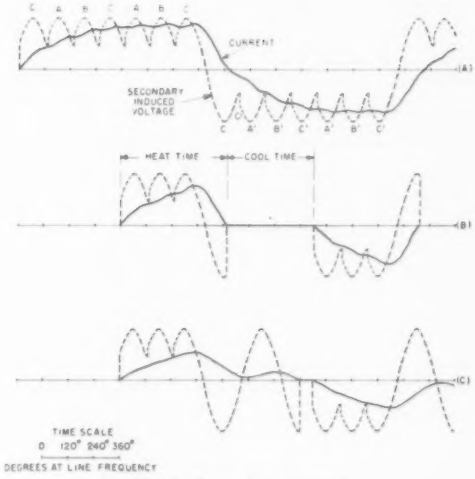

Fig. 5 Secondary current

circuit resistance and inductance, respectively.

SECONDARY CURRENT

Typical secondary current waves are illustrated by Fig. 5. The corresponding secondary voltages are shown by dashed lines.

The peak value of secondary current may be obtained from the following formula

$$I_s = \frac{E_{do}}{R + (3X_{pp}/4\pi N^2)} \qquad (3)$$

where

I_s = peak secondary current.
R = total resistance, referred to secondary, of one primary and the secondary circuit, including the weld.

If R and X_{pp} are expressed in ohms, the term $(3\,X_{pp})/4\,\pi\,N^2$ will usually be from 5 to 30% of R. The value R, is for low-frequency operation. Tests have indicated that R is slightly less than the resistance obtained by tests at 60 cycles.

It may be noted that as heat adjustment is made by ignition retard, I_s is reduced in the same manner as E_{do}. Hence the heat setting is 100% times cos a.

The rms. value of secondary current measured over one heat time may be determined by the following formula

$$i_s = \frac{KE_{do}}{\sqrt{[R + (3X_{pp}/4\pi N^2)]^2 + X^2}} \qquad (4)$$

where

i_s = rms. secondary current measured over one heat time.
X = reactance of welder at the maximum secondary frequency (f) obtainable with the heat time employed.
K = factor[8] to account for squareness of secondary voltage wave. Approximately, $K = 0.91$, for most cases (when X/R is between 0.5 and 1.5).

It will be noted that voltage corresponding to the last conducting primary is generated in the secondary until the current goes to zero. Hence the voltage may go below zero, as shown by Fig. 5 (*B*). This figure shows that the heat time with load is somewhat longer than the no-load heat time shown on Fig. 3 (*B*).

For Fig. 5 (*B*), the current went to zero before the secondary voltage returned to a positive value. However, if the secondary L/R is sufficiently great, the secondary voltage may return to a positive value, increasing the current until after the voltage again goes negative. This is shown by Fig. 5 (*C*). The secondary current thus may continue to flow for one or more primary cycles after tube ignition has been cut off. This condition causes one or more extra humps of current, and may occur whenever the welder L/R exceeds about 0.64 cycles of the line frequency. This corresponds to welders which would have a power factor less than about 24% when using a single-phase transformer.

Such humps may usually be avoided by firing Tube A slightly before the voltage of Phase C returns to a positive value.

ARC-THROUGHS

An arc-through will occur if ignition of the reverse tubes occurs before the forward current goes out.

An example of such a fault is illustrated by Fig. 6. It will be seen that a large fault current flows through Tube C. This current is similar in magnitude and wave shape to arc-back current[9] in a 3-phase rectifier.

The normal starting instant for the first reverse tube

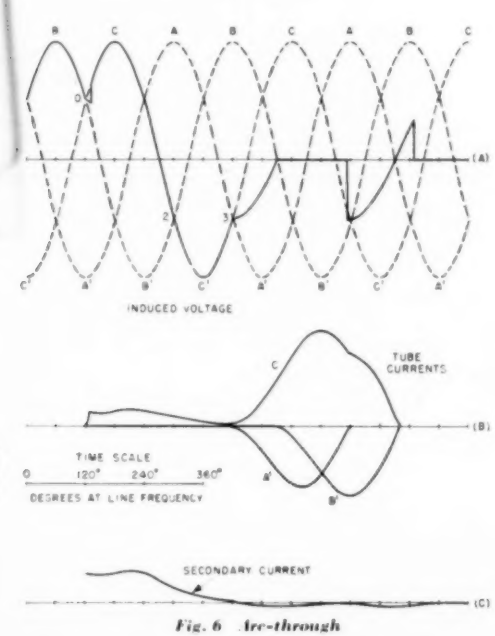

Fig. 6 Arc-through

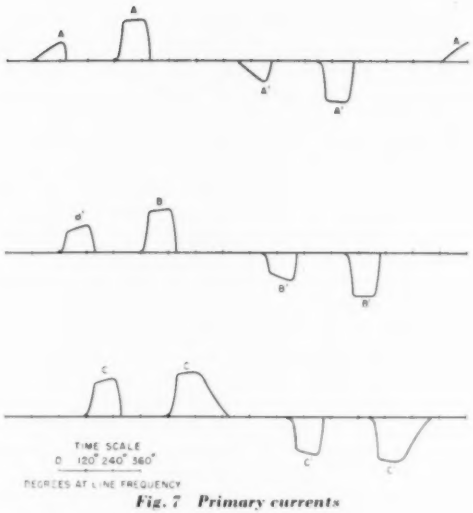

Fig. 7 Primary currents

(*A'*) is at Point 3, Fig. 6. The current in the last forward tube (*C*) will go out before this point if the welder L/R is less than about 0.55 cycle of the line frequency. This corresponds to welders which would have a power factor of more than about 28% when using a single-phase transformer.

If this power factor would be less than about 28%, a cool time, or other provision for avoiding arc-throughs, should be used.

PRIMARY CURRENT

Typical primary currents are shown on Fig. 7.

Each primary carries current $^1/_3$ of the time (plus the short time of commutation). Also, the instantaneous magnitude of the primary current is equal to the secondary current divided by the turn ratio, since saturation can usually be neglected. Hence, the rms. primary current is approximately

$$i_p = \frac{i_s}{N\sqrt{3}} \qquad (5)$$

It may be noted from Fig. 7 that Phase C carries slightly more current than the other phases.

For heat times exceeding 2 line-frequency cycles, the deviation from the current value of Formula 5 is only about 10% or less, except when there are extra humps of current in the last conducting phase. As the heat time increases, phase currents become more nearly balanced.

LINE CURRENT

The current in each line is the difference of the currents for the two primaries to which the line is connected. Typical line currents are shown by Fig. 8. Figure 9 is an oscillogram showing line currents together with the corresponding secondary current.

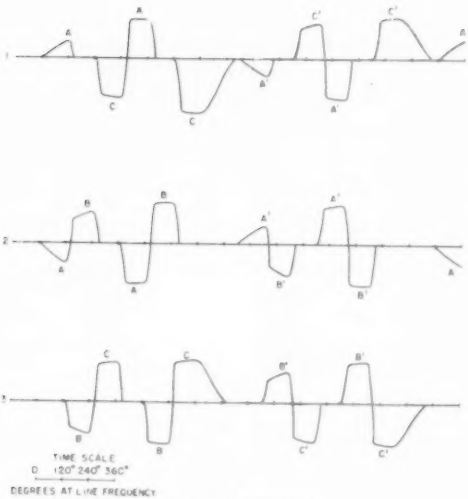

Fig. 8 Line currents

The peak value of line current is

$$I_{LP} = I_s/N \qquad (6)$$

When the secondary current is fully built up, the line current equals this peak value during $^2/_3$ of each line-frequency cycle, (neglecting the time of commutation). Hence the rms. line current, when secondary current is fully built up, is

$$I_L = \sqrt{\frac{2}{3}}\, I_{LP} = 0.815\, I_{LP} \qquad (7)$$

Over one heat time the rms. line current is less than this maximum rms. value, because the rms. secondary current is less than the secondary peak current. The rms. line current over one heat time is approximately

$$i_L = I_L \left(\frac{i_s}{I_s}\right) \qquad (8)$$

In typical cases i_L is from 70 to 95 % of I_L.

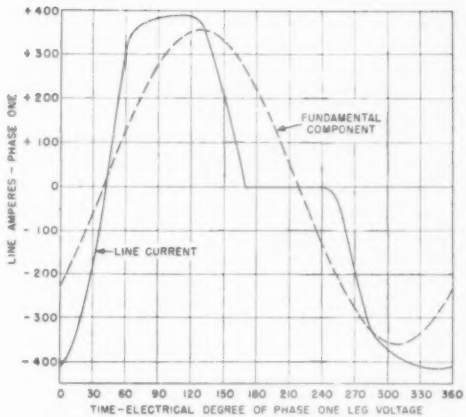

Fig. 10 Line current to 3-phase welder

Figure 10 shows a line current trace (solid line) from another typical test oscillogram during the time when the output current is fully built up, plotted for a period of one line-frequency cycle. In this same figure is plotted the line frequency component of the wave, obtained by harmonic analysis. The difference, between this line frequency component and the actual wave, consists of harmonics.

The shape of the current wave shown by Fig. 10 is very similar to that of a half-wave, 3-phase rectifier delivering constant d.-c. current.

Therefore we may use the following formula for the rms. value of the line-frequency component of line current, when secondary current is fully built up.

$$I_{LF} = \frac{3}{\pi\sqrt{2}}\, I_{LP} = 0.674\, I_{LP} \qquad (9)$$

Variations in commutation time have a negligible effect on this value.

If heat is reduced by ignition retard (phase control) of each tube, the line current wave shape remains ap-

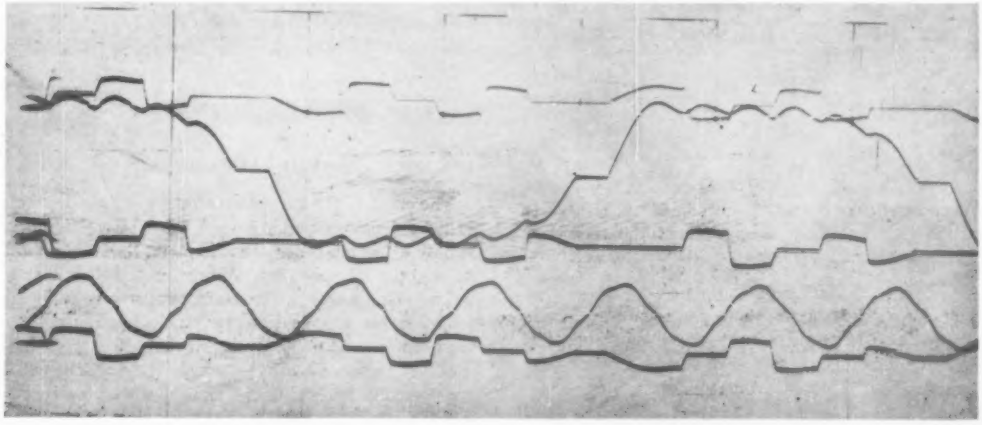

Fig. 9 Oscillogram showing line current, line voltage and secondary current

proximately the same, although its magnitude is reduced in proportion to the reduction in I_s, and the wave is shifted in a lagging direction. Consequently, formulas 6–9 may be used for reduced heat settings, by using the correspondingly reduced value of I_s.

When the secondary current is not fully built up, the wave shape is different than that shown in Fig. 10, so that Formula 9 does not apply over an entire heat time. Over this time, the rms. line-frequency component is somewhat less than I_{LV}. It is approximately

$$i_{LV} = I_{LV}(i_s/I_s) \qquad (10)$$

According to formulas 7 and 9, the totals rms. value of the line current is 21% higher than the rms. value of the line frequency component. This 21% is the contribution of harmonics to the rms. current. When commutation time is considered, this contribution is found to vary from 10 to 20% in practical cases.

Because the primary phase currents are slightly unbalanced, the rms. line currents over one heat time also differ slightly from each other. However, unbalance is usually less than 10% and can be neglected for most practical purposes.

KVA

Assuming that the load is balanced, we may use the following formula for the total rms. kva., when secondary current is fully built up

$$(KVA)_L = \sqrt{3}EI_L \qquad (11)$$

The rms. kva. over one heat time is approximately

$$(kva)_L = (KVA)_L(i_s/I_s) \qquad (12)$$

The line-frequency component or rms. kva. is called the vector[10] kva. Therefore the vector kva., when secondary current is fully built up, is

$$(KVA)_{LV} = \sqrt{3}EI_{LV} \qquad (13)$$

The rms. value of vector kva. over a heat time is approximately

$$(kva)_{LV} = (KVA)_{LV}(i_s/I_s) \qquad (14)$$

It is of interest to note from formulas 1, 6, 9 and 13 that

$$(KVA)_{LV} = \frac{E_{do}I_s}{\cos a} \qquad (15)$$

POWER

When secondary current is fully built up, the power input is the secondary voltage times the secondary current.

$$P = RI_s^2 = E_dI_s \qquad (16)$$

This maximum power value includes transformer losses because R includes transformer resistance and E_d does not include transformer resistance drop.

Over one heat time, the average power is

$$p = P\left(\frac{i_s}{I_s}\right)^2 \qquad (17)$$

In typical cases the average power, p, over one heat time is from 50 to 90% of the maximum power, P.

POWER FACTOR

Power factor equals the power divided by the rms. kva. Hence, when the secondary current is fully built up, the line input power factor is

$$(PF)_L = \frac{P}{(KVA)_L} \qquad (18)$$

The power factor of the line-frequency component of current is called the vector[10] power factor. It equals the power divided by the vector kva. Hence, when secondary current is fully built up, the line input vector power factor is

$$(PF)_{LV} = \frac{P}{(KVA)_{LV}} \qquad (19)$$

The vector power factor is also equal to the cosine of the angle between the line-frequency component of current and the corresponding line-to-neutral voltage. In the example illustrated by Fig. 10, the angle of lag is seen to be 39°, so that the vector power factor is 78%.

When secondary current is fully built up, the full-heat vector power factor of typical frequency-changer welders varies from 75 to 95%. When heat is reduced by ignition retard, the vector power factor is reduced from the full-heat value in the same proportion as current is reduced.

The vector power factor may be obtained from the following approximate formula, which applies when secondary current is fully built up.

$$(PF)_{LV} = \left(\frac{R}{R + 3X_{pp}/4\pi N^2}\right)\cos a = \frac{E_d}{E_{do}}\cos a \qquad (20)$$

The power factor is less than the vector power factor by the distortion factor resulting from the line current harmonics. This factor varies from 10 to 20% in most cases. Based on power rectifier practice, an approximate formula for this distortion factor is

$$D = \frac{(PF)_L}{(PF)_{LV}} = \frac{\sqrt{3}}{2}\frac{(3)}{(\pi)} = 0.826 \qquad (21)$$

The average power factor and average vector power factor over one heat time have not been evaluated.

VOLTAGE DROP

The effect of the harmonics on voltage drop is practically negligible, especially when the total voltage drop is only a few per cent. Furthermore, harmonics tends to slightly increase rms. voltage, and hence slightly reduce voltage drop. Therefore, a satisfactory approximation to the voltage drop can be calculated by neglecting harmonics entirely.

When harmonics are neglected, the line current is considered to be a sine wave, and the corresponding sine waves for the three phases are displaced 120 electrical degrees from each other. Their magnitudes are very

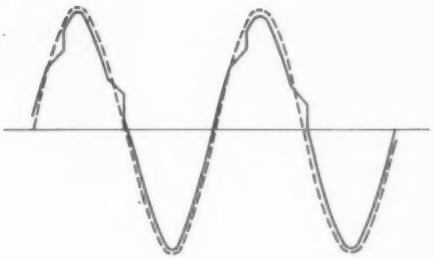

Fig. 11 Line voltage

nearly the same. Consequently, the voltage drop may usually be calculated with good accuracy on the basis that the load is a balanced, 3-phase, sine-wave load, with a magnitude equal to that of the line-frequency component of current.

When the voltage drop is only a few per cent, a simple calculation of this drop can be made after separating the vector kva. into its power and reactive components using the vector power factor, and determining their per unit values on a selected base kva. The calculated per cent voltage drop is the per cent line reactance times the per unit reactive kva. plus the per cent line resistance times the per unit power.

For most frequency-changer welders, maximum rms. voltage drop usually occurs at 100% heat setting.

The rms. voltage drop over one heat time may be evaluated with reasonably good accuracy by using the rms. vector kva., $(kva)_{LV}$, over this time and the vector power factor, $(PF)_{LV}$ at the time where secondary current is fully built up.

When the supply line impedance is largely reactance, it may increase commutation time appreciably if the voltage drop is more than a few per cent. This lowers the vector power factor and causes the voltage drop in such cases to slightly exceed the drop calculated by the formulas presented. In most practical cases, however, this effect is negligible.

The voltage drop has a rather irregular wave form because most of the reactive drop occurs during commutation. This is illustrated by Fig. 11. The instantaneous drop may thus exceed the rms. value. The maximum instantaneous drop can be calculated, if necessary, by methods based on power rectifier practice; but the formulas to be used are not presented in this paper.

COMPARISON WITH CONVENTIONAL WELDERS

On the basis of the analysis presented, a means of approximate comparison of frequency changer welders with conventional single-phase welders is available.

We might for example, compare a single-phase, 60-cycle welder with a 3-phase frequency changer welder having the same arms and electrodes, and the same rms. current output, over one heat time, welding the same material. In this case we might make the assumption that the resistance r of the 60-cycle machine when referred to the secondary is approximately equal to the low-frequency resistance, R, of the frequency-changer machine plus the term $(3X_{pp}/4\pi N^2)$ used in Equation 3 to account for the effect of commutation on output. On the basis of this assumption, and assumed equal machine inductances, we can obtain relatively simple relationships for comparative inputs to the machines.

In order for the two machines to have the same rms. output current over one heat time, the peak secondary current of the frequency changer machine must be higher than the rms. secondary current of the 60-cycle machine by the following ratio.

$$\frac{I_s}{i_s} = \frac{\sqrt{r^2 + (fx/60)^2}}{r}\left(\frac{1}{K}\right) \qquad (22)$$

The resistance r, and reactance x, are measured at 60 cycles at the primary of the single-phase welder. The resistance, r, includes the weld resistance.

The required secondary no-load voltage of the frequency changer welder will be lower than that of the 60-cycle machine, as follows

$$\frac{E_{do}}{E_{60}} = \frac{I_s}{i_s}\frac{r}{\sqrt{r^2 + x^2}} = \frac{I_s}{i_s}(PF)_{60} \qquad (23)$$

The power factor, $(PF)_{60}$ of the 60-cycle machine is measured while welding the material considered.

The resistance of the welded material has a pronounced effect on the required secondary voltage of a frequency changer welder. Consequently, if the welders being compared are to be used for several different types of material, it is advisable to consider each separately, in order to assure that the frequency changer welder has sufficient secondary voltage rating.

The maximum input vector kva. demand of the frequency changer welder will be smaller than the demand kva. of the 60-cycle machine, as follows, if both are at their maximum secondary voltages and delivering the same rms. current over one heat time (that is, using the material which requires maximum secondary voltage of the frequency-changer machine):

$$\frac{(kva)_{LV}}{(kva)_{60}} = \frac{(E_{do}I_s)}{(E_{60}I_{60})} = \left(\frac{I_s}{I_{60}}\right)^2(PF)_{60} \qquad (24)$$

Over one heat time, the rms. vector kva. values will compare as follows

$$\frac{(kva)_{LV}}{(kva)_{60}} = \frac{I_s}{I_{60}}(PF)_{60} = E_{do}/E_{60} \qquad (25)$$

The rms. short-circuit kva. of the frequency-changer over one heat time is as indicated below.

$$\frac{(kva)_{LV}}{(kva)_{60}} = \left(\frac{E_{do}}{E_{60}}\right)\frac{\sqrt{r^2 + (fx/60)^2}}{\sqrt{\mathbf{r}^2 + (f\mathbf{x}/60)^2}} \qquad (26)$$

All values are measured at short circuit. The last part of this formula is based on the assumption that the

reactance x, during welding, is the same as the reactance, $\mathbf{x}$, at short circuit.

As an example of comparison of two machines, consider a 12-cycle frequency-changer welder to replace a 60-cycle, single-phase machine having a 440-v. kva. demand of 300 kva. at 50% *pf* when welding steel and 330 kva. at 29% *pf* at short circuit. From these single-phase demand and *pf* data we may say that r, $\mathbf{r}$, x and $\mathbf{x}$ are 0.33, 0.17, 0.56 and 0.56 ohms, respectively.

From Formula 22 the ratio I_s/i_s is 1.16. From Formula 23, the ratio of E_{do}/E_{60} is 0.58. From Formula 25, the 3-phase vector kva. demand $(kva)_{LV}$ of the frequency-changer machine is 175 kva. over one heat time while welding steel. This is only slightly over one-half the corresponding demand of the 60-cycle machine.

At short circuit, however, the rms. vector kva. of the frequency changer is 290 kva., by Formula 26. If harmonics are included, the total kva. will be about 330 kva.

The short-circuit kva. of this frequency-changer welder is thus practically the same as for the 60-cycle machine, and the secondary short-circuit current is much greater.

The vector power factor of the frequency-changer welder will be better, however, than for the 60-cycle machine, and the demand will be 3-phase instead of single-phase.

Consequently, even with the same demand kva., the voltage drop of the frequency changer welder will be less than half that for the 60-cycle machine.

If a conventional welder and a frequency-changer welder each deliver the same rms. welding current to high-resistance material, such as steel, the frequency-changer machine will deliver much more to low-resistance material, such as aluminum. On the other hand, if the two machines deliver the same rms. welding current to aluminum, the frequency-changer welder will deliver much less to steel. The current output of the 60-cycle machine, on the other hand, will not be so greatly affected by the resistance of the material welded.

The maximum input kva. and output current of either machine will be delivered when the electrodes are short circuited, and will be, in either case, only slightly more than when welding aluminum.

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Appendix A—Tube Currents

Each tube carries that part of the current of its primary which is in one direction.

For tube selection, the average tube current determines tube heating.

As in the case of primary currents, the average current in one tube is sometimes slightly higher than for the other two.

Approximately, however, the average tube current is

$$i_T = \frac{i_s}{6N} \qquad \text{(A-1)}$$

and the maximum tube current is

$$I_T = \frac{I_s}{N} \qquad \text{(A-2)}$$

Appendix B—Tube Inverse Voltage

When no tubes are conducting, the anode-to-cathode voltage of each tube is equal to the line-to-line voltage of the phase in which the tube is connected.

When one tube is conducting, this causes the corresponding line-to-line voltage (less tube drop) to be applied to its primary winding. There is then induced in the other two primary windings, a voltage practically equal to this applied voltage, and in phase with it. (Primary resistance drop and rate of change of secondary can usually be neglected.)

This induced voltage subtracts from the line voltage of the corresponding other line phases to give an anode-to-cathode voltage across the tubes in their other phases. When only one tube is conducting, the resulting net voltage is an a.-c. voltage with peak value equal to 1.73 times the peak line voltage.

Hence, the peak tube voltage is

$$E_T = \sqrt{2}\sqrt{3}\,E = 2.45E \qquad \text{(B-1)}$$

For a 440 supply this peak tube inverse voltage is 1080 v.

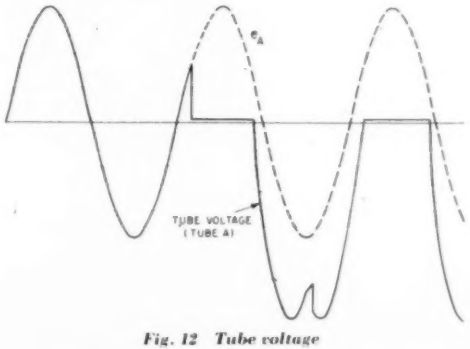

Fig. 12 Tube voltage

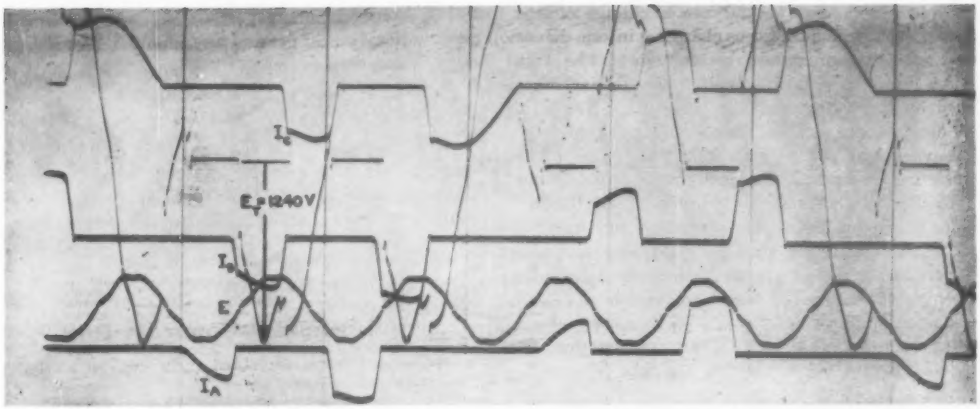

Fig. 13 Oscillogram showing primary-currents, line voltage and voltage across one tube. Supply voltage is approximately 480 volts rms.

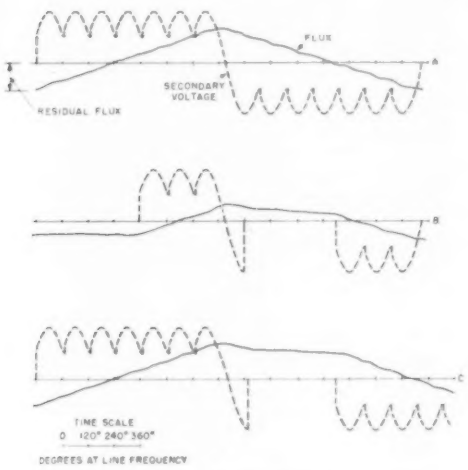

Fig. 14 Transformer core flux

When two tubes are conducting, the induced voltage of the remaining phase is equal to the average of the voltages for the two conducting phases. This causes the tube inverse voltage to be lowered somewhat during commutation as shown by Fig. 12. As shown by this figure, the wave shape of the tube inverse voltage is practically the same as for a 3-phase Y rectifier with an applied line-to-line voltage 73% higher than the welder line voltage.

Tubes should be selected in a manner analogous to tube selection for such a rectifier.

Figure 13 shows an oscillogram of the voltage across one tube of a frequency-changer welder.

Appendix C—Transformer Flux

A typical variation of transformer flux is illustrated by the dashed lines of Fig. 14 (*A*). The initial flux is the residual value left from previous operation. The corresponding residual flux density is shown by point 1 of the typical hysteresis loop of Fig. 15. The induced

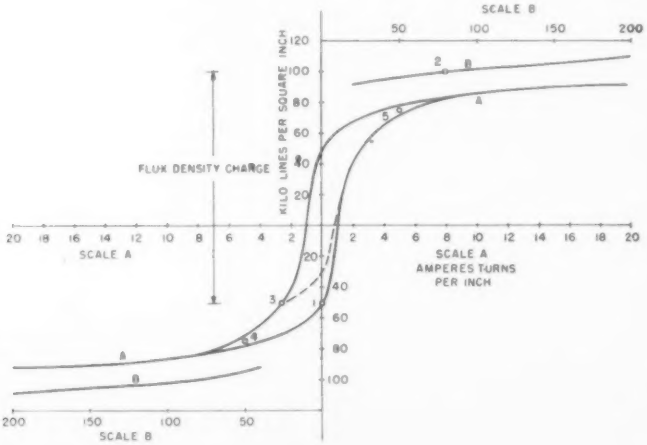

Fig. 15 Typical hysteresis loop

voltage corresponds to the rate of change of flux linkages, so that the flux keeps changing in one direction as long as the voltage has one polarity. The total flux change during a secondary half cycle is thus determined by the voltage and the heat time. The total flux density change is indicated on Fig. 15, and the final point reached is Point 2 on this figure. The transformer design should be such that the total change of flux, from the residual value, does not cause the final value, at the end of a low-frequency half-cycle, to excessively saturate the core. It is not necessary that the core be completely unsaturated, since magnetizing current is very much less than load current.

The next half-cycle of voltage, being of the opposite polarity, corresponds to a rate of flux change toward the original residual value. Hence, the flux follows the hysteresis loop to Point 3 of Fig. 15. Point 3 is very slightly nearer saturation than Point 1, because of the primary resistance drop due to magnetizing current required near Point 2, which reduces the voltage and the flux density change from 1 to 2 slightly below the values from 2 to 3. After many cycles, this tendency causes the path traversed to extend between points 4 and 5. If primary current is cut off, with the secondary closed, the magnetizing current required to maintain flux at Point 4 flows in the secondary, but decays gradually and the flux density returns to the residual Point 1. If the welder electrodes are open circuited, before the residual flux value is reached, sparking will occur at the electrodes, which will drive the flux quickly to this residual value. The slow decay of flux after primary current cut-off, with the secondary closed, is illustrated during the cool times shown on Figs. 14 (*B*) and 14 (*C*).

The maximum flux of Fig. 14 (*B*) is less than for Fig. 14 (*A*) because the heat time is less, permitting less total flux change.

Figure 14 (*C*) shows that lowering of the frequency by delaying the starting of secondary pulses does not increase transformer flux requirements. Also, extra humps of current (if any) do not increase the maximum flux.

There is therefore a basic secondary frequency one half-cycle of which is the heat time. This basic frequency may never occur in practice, unless zero cool time is used, but it forms a basis for transformer design.

The transformer flux change of Fig. 14 (*A*) may be determined by the following formula.

$$\phi_C = \frac{E_d}{2N_s f} 10^8 \qquad \text{(C-1)}$$

where

N_s = secondary turns
ϕ_C = flux change, in lines

The maximum flux density is

$$\phi_M = \phi_C - \phi_R \qquad \text{(C-3)}$$

where

ϕ_M = maximum flux
ϕ_R = residual flux

When the effective air-gap at core joints is small, and flux densities are below saturation, in highly retentive steel, ϕ_R approximately equals ϕ_M. Since E_d is usually nearly E_{do}, the approximate maximum steady state flux then is

$$\phi' = \frac{E_{do} \times 10^8}{4N_s f} \qquad \text{(C-4)}$$

The frequency f is the maximum secondary frequency obtainable with the heat time employed. Typical values are 12 and 8.6 cycles per second for no-load heat times of about 2 and 3 cycles, respectively.

Appendix D—Nomenclature

a	—angle of retard (heat control setting = $100^\circ_C \cos a$).
D	—distortion factor of primary current.
E	—line-to-line rms. voltage.
E_{do}	—secondary average no-load induced voltage.
E_{60}	—secondary voltage of comparable 60-cycle machine.
E_T	—peak tube inverse voltage.
f	—basic secondary frequency.
I_{LP}	—peak line current.
I_L	—maximum rms. line current.
I_{LV}	—maximum fundamental rms. line current.
i_L	—rms. line current over one heat time.
i_{LV}	—fundamental rms. current over one heat time.
i_p	—rms. primary current over one heat time.
I_s	—maximum secondary current.
i_s	—rms. secondary current over one heat time.
I_T	—peak tube current.
i_T	—average tube current.
K	factor to account for effect on secondary rms. current of squareness of secondary voltage wave ($K = 0.91$)
$(KVA)_L$	—maximum rms. kva. input.
$(KVA)_{LV}$	—maximum vector kva. input
$(kva)_L$	—rms. kva. over one heat time.
$(kva)_{LV}$	—rms. vector kva. over one heat time.
$(kva)_{60}$	—demand kva. of comparable 60-cycle welder.
N	—primary turns per secondary turn (for one primary).
P	—maximum power input.
p	—average power input.
$(PF)_L$	—power factor of line input.
$(PF)_{LV}$	—vector power factor of line input.
$(PF)_{60}$	—power factor of comparable 60-cycle welder.
R	—resistance of weld, welder, secondary and one primary (referred to secondary).
r	—resistance of comparable 60-cycle welder.
X	—secondary reactance (at basic secondary frequency, f).
x	—60-cycle reactance of comparable 60-cycle welder.
X_{pp}	—average reactance between primaries (at primary frequency).

Bold face type indicate a quantity is measured with electrodes short-circuited.

Additional symbols used in Appendix C are identified therein.

Lowering Cost of Welded Construction

Through the selection of larger electrodes, use of higher currents, intermittent welding and smaller size fillets considerable savings will result. These savings can be further augmented by suitable bending, fitting and cutting operations and the use of subassemblies

by Omer Blodgett

IN these days when everyone is becoming more cost conscious, it would be well to look into the cost of welding and review some of the factors which would allow us to reduce this cost. There are many who do not fully realize the cost of welding. Table 1 shows the cost of depositing 1 lb. of weld metal for the various sizes of electrode. We must realize that the welding operator is paid to melt electrode; the joint is made by depositing weld metal. On an average 3 lb. of electrode must be melted in order to give us a weld deposit of 2 lb. because approximately 30% is lost through spatter, stub ends and coating. This table is based upon $1.25 per hour for operator, 200% for plant overhead, 2¢ per kwh. for power and 50% operating factor. Notice the rapid decrease in cost of welding due to the use of higher currents and larger electrode. The cost of depositing 1 lb. of weld metal using 1/8-in. electrode is about $4.50 per pound. If 5/32-in. electrode were used the cost would be $3.60. If 3/16-in. electrode were used this cost would drop to $3.00. A 1/4-in. electrode would further reduce this cost to $1.60, and if 5/16-in. electrode would be used this cost would be lowered to $1.20. Using the automatic submerged arc, the cost would drop to less than 50 ¢ per pound.

In Fig. 1 is shown a 1/4-in. fillet weld, half of which was made with a 1/4-in. E6012 electrode, 325 amp. at 10 in. per minute. The other half was made with a 3/16-in. electrode, 200 amp. at 5 in. per minute. Both welds are as strong and we are able to save 50%.

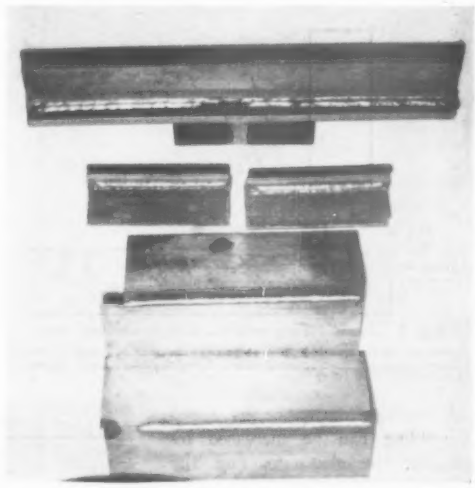

Fig. 1 Same size fillet welds made with 1/4 in. and 3/16 in. electrodes

In the same figure is shown an overhead fillet weld made with 5/32-in. E6013 electrode at 160 amp. and a similar weld made with 7/32-in. E6013 electrode at 240 amp. The first weld took 70 sec. and the second took 50 sec., or a saving of 29%. A corner weld of 16-gage sheet metal is also shown. It is welded in one case with 3/32-in. E6013 electrode at 60 amp. with a speed of 15 in. per minute and in the second case using 5/32-in. E6010 electrode at 90 amp. and 40 in. per minute resulting in a saving of 60%. In general it is safe to say that if a joint can be welded using just one size larger electrode it will result in a 25% saving. With

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Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

Table 1

	1/8 in.	5/32 in.	3/16 in.	1/4 in.	5/16 in.	*Submerged arc automatic*
Approximate current	110	130	160	250	350	800
Cost per pound deposited metal	$4.50	$3.60	$3.00	$1.60	$1.20	Less $0.50
Cost per 50-lb. box	$150	$120	$100	$55	$40	Less than $25

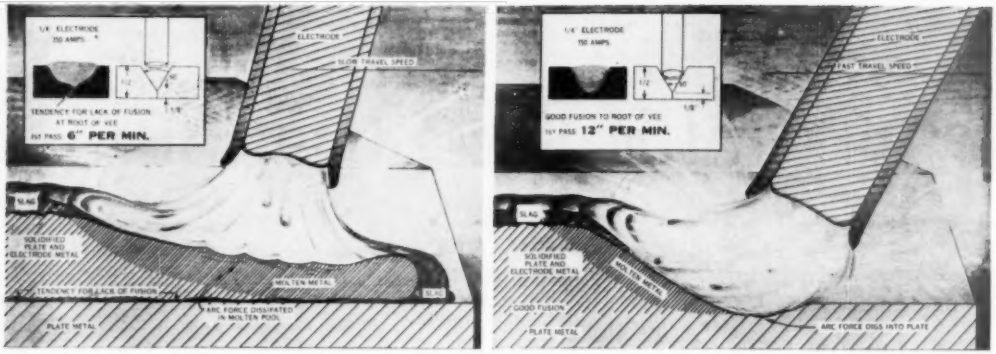


Fig. 2 (*Left*) *Effect of slow travel speed on penetration. Cross section of arc in 60° vee groove.* (*Right*) *Effect of fast travel speed on penetration. Cross section of arc in 60° vee groove*

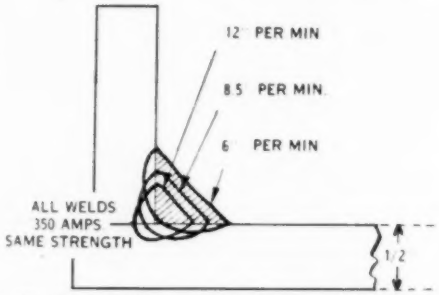


Fig. 3 A small deep weld vs. a large shallow one

Speed can be increased by decreasing the amount of electrode metal in joint through deep penetration. Cross-hatched area is metal added to outside of joint

high welding currents, the speed of welding can be increased by dragging the electrode, and the throat of the fillet will be the same as that made with a slower travel speed, for the same current. Although this faster travel results in less weld metal being added, the weld retains its same depth of throat because of its increased penetration (Figs. 3 and 4). As this travel speed is increased the arc gradually gets ahead of the molten metal thereby penetrating deeper into base metal instead of wasting its heat on molten weld metal (Fig. 2).

It might be interesting to consider what a box of welding electrode represents in total cost of welding, including, in addition to the cost of the electrode, the cost of labor, overhead and power. Normally a 50-lb. box of electrode would cost about $4.25. This cost of $4.25 is all we consider when we purchase it; however, if we were to put price tags on the electrodes showing what it is going to cost us to deposit this box of electrode, we would see the importance of using as large electrodes as possible. With this in mind the 50-lb. box of $^1/_8$-in electrode would cost us about $150; $^5/_{32}$-in. electrode, $120; $^3/_{16}$-in. electrode, $100; $^1/_4$-in. electrode, $55; $^5/_{16}$-in. electrode, $40, and automatic submerged arc would be less than $25 for the same

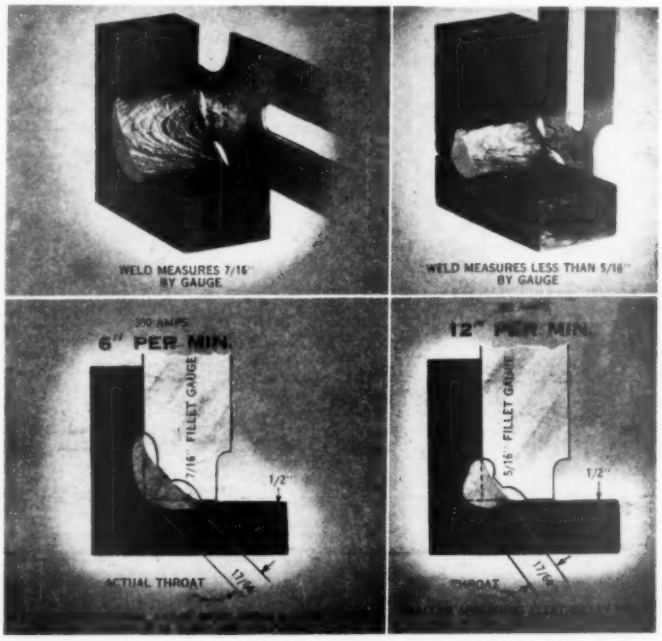


Fig. 4 Why gage size does not indicate true strength

"The fillet" weld on right measures slightly less than $^1/_{16}$in. by gage. Conventional flat fillet on left measures $^7/_{16}$ in. by gage. Since both welds are of equal strength, this shows that gage size of fillet welds does not indicate the true weld strength. Lower illustrations are end views of specimens shown above

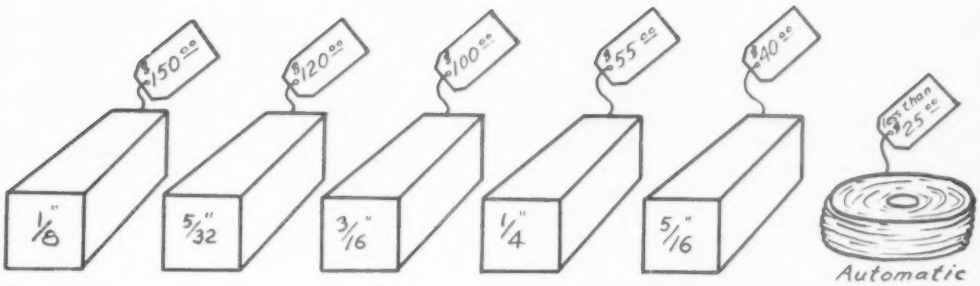


Figure 5

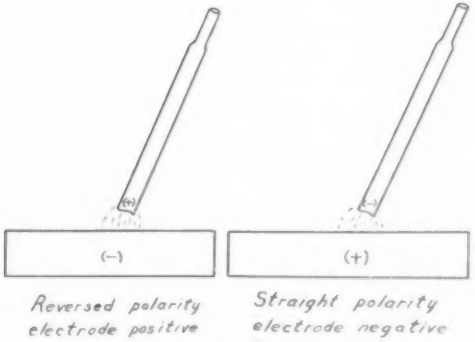


Figure 6

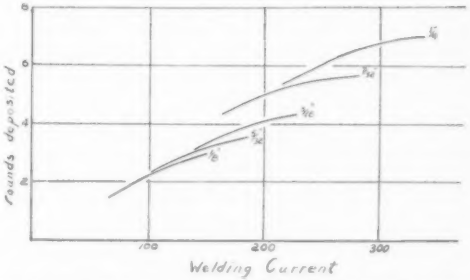


Fig. 7 Higher currents permissible with larger electrodes

50 lb. (Fig. 5). Of course, we still have a further advantage in submerged arc welding; that is, on an average it requires only one-half to one-third the weld metal as hand welding for the same joint because of its deeper penetration. Welding is not expensive but money can be saved through using good procedures.

Since all electric fusion welding consists of depositing weld metal and fusing it into base metal in order to obtain a joint, the greatest possible speed of welding results when the maximum amount of weld metal is deposited along with maximum penetration of base metal per unit of time.

For the main part, the electrode is melted from the heat produced by the product of the welding current, I, and the voltage drop across the surface of the tip of the electrode, E. Heat (watts) = $E \times I \times$ Time, where I = welding current and E = electrode surface drop (Fig. 6). This electrode surface voltage drop can be changed by the nature of the coating of the electrode and the polarity used in welding. In coated mild steel electrodes the negative side has the greatest drop, hence straight polarity (in which the electrode is negative) will have a faster rate of melt-off for a given current than reversed polarity; however, reversed polarity will cause the work to be hotter than in straight polarity, for the same current, resulting in deeper

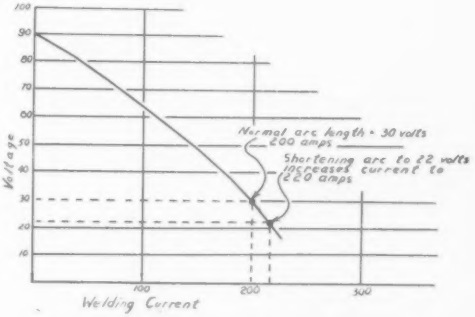


Fig. 8 Characteristic curve of welding generator

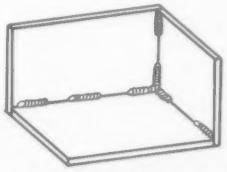


Fig. 9 Short intermittent welds can often be utilized

penetration and more tendency for burn-through if there is poor fit-up. Of course, there are times when this deeper penetration due to reversed polarity is desired; such as butt weld without bevel.

Once the polarity is decided, the only variable factor left for a higher melt-off rate is current used. Generally, the size of electrode has little to do with melt-off rate for a given current. A larger electrode simply allows us to use a higher current (Fig. 7). For a given setting of the welding generator, a shorter arc will result in a slightly higher current because of the lower resistance of the shorter arc, thus allowing more current to flow. See Fig. 8, which is a characteristic curve of a welding generator. One thing we sometimes forget is that electrode melted is not equal to weld metal deposited and, of course, the deposited weld metal is what we are interested in. The more current we use on a given size electrode, the greater will be the spatter. Normally this is between 10 and 15%. You can see that very high currents might make it possible to deposit less weld metal with a further increase of current because of increased weld spatter.

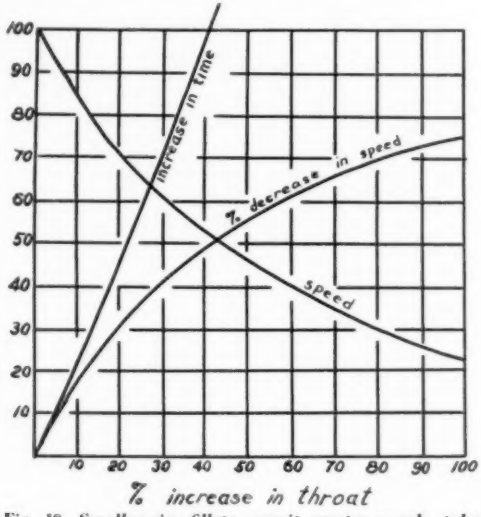


Fig. 10 Smaller size fillets permit greater speeds, take less time and less weld metal

Do not overweld. In many cases it is not necessary to make continuous welds, since only small, short intermittent welds are required (Fig. 9). This will lower the cost considerably. Oftentimes welds are made too heavy because the weld size has not been marked on the print by the engineering department or the operator cannot judge weld size properly, making the weld much heavier than is necessary in order to be on the safe side. In Fig. 10 we see that even a 15% increase in weld size will cause a 24% decrease in speed and require 32% more time to put in a given weld. Going to extremes, if the throat size of a fillet weld is doubled, the speed will be one-fourth and it will require four times the length of time to deposit it.

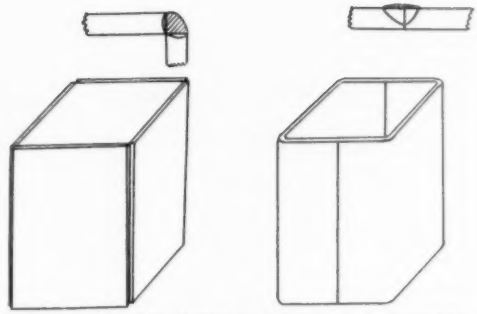


Fig. 11 Change in design reduces amount of welding necessary

Whenever possible it is recommended that metal be bent to eliminate a corner weld. In Fig. 11 is shown a base which was changed from a design using side plates, joined by four corner welds, to a design using two plates with their corners broken and joined with two butt welds. Not only has the number of welds been reduced, but welding time is further reduced because butt welds require less weld metal and are made in less time. Electrodes have been developed during the war and since which can be used with higher welding heats without excessive spatter; the welds are smoother and therefore slag removal is easier. Fillet welds are made fast without undercutting. In fact, the

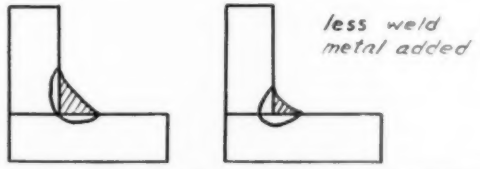


Fig. 12 Higher current, greater speeds; deeper penetration, less weld metal

electrode may be dragged; that is, the coating on the electrode actually touches the sides of the plate while welding. This gives us much deeper penetration, thus requiring less weld metal for a given size weld. This is shown in Fig. 12. Since less weld metal is used, greater welding speeds are obtained. In addition to this,

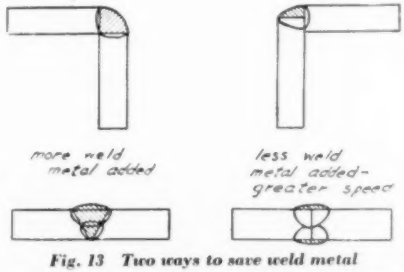


Fig. 13 Two ways to save weld metal

Plate edges in the corner joint are overlapped so that more of the base metal melts. More base metal also melts in the unbeveled butt weld

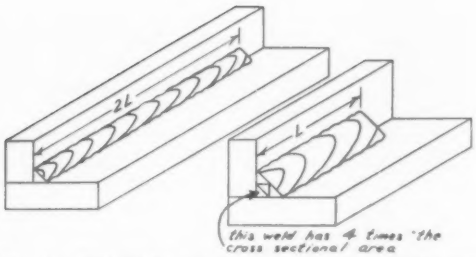


Fig. 14 Both welds have the same strength, yet the one on the left requires only half as much weld metal and can be made at twice the speed of the shorter weld on the right

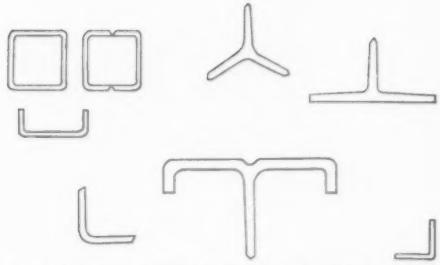


Fig. 16 I-beams, channels, angles, T-sections and other sections made with rounded corners are now available from steel suppliers and enable a smooth flow of stress to be provided in a welded structure

less distortion is encountered because less heat enters the base metal due to the increased welding speed, and also since we get deeper penetration, the shrinkage in the fillet weld has less moment arm with which to pull.

Always use a joint design which allows the least amount of weld metal. As less weld metal is used, less time is required for the welded joint. Figure 13 shows the reduction of weld metal in a corner joint by allowing the edges of the plate to lap slightly so that more of the base metal melts and becomes part of the joint. Also shown is a butt joint in which there is no joint preparation, requiring only sufficient weld metal for reinforcement, although the weld is made all the way through the plate. It is evident from comparison that this joint uses less weld metal than one having beveled edges.

Many times large intermittent welds may be replaced with smaller welds which are longer. This is shown in Fig. 14 which compares a $^1/_2$-in. fillet weld, 3 in. long, and a $^1/_4$-in. fillet weld, 6 in. long. It is seen that the $^1/_2$-in. weld is just half as long as the $^1/_4$-in. fillet weld, but because the $^1/_2$-in. weld has four times the cross-sectional area the net effect is that one-half of the weld metal is saved by making the $^1/_4$-in. weld 6 in. long, yet this has the same strength as the heavier weld.

Since higher welding currents may be used for flat positioned welding it is obvious that as much of the welding as possible should be positioned for this type of welding for maximum speed. By this means larger electrodes and higher currents may be used. This is shown in Fig. 15. Since less time is needed for welding there will be less distortion.

There is a trend now for the steel suppliers to offer a great variety of sections and shapes which will greatly aid fabricators of welded steel construction. Such sections as I-beams, channels, angles, tee sections, etc., made with round radius are shown in Fig. 16. These could be readily welded into a structure and permit maximum fatigue strength because of the smooth flow of stress throughout the section.

Many times finished welds are ground smooth when it is not necessary. As this unnecessary grinding time usually is greater than the actual welding time, it represents a needless increase in cost of fabrication. Those familiar with cast construction may feel that welds must be ground smooth in order to duplicate the original cast construction. The fitter who is cutting the plates and erecting the structure is just as responsible for low-cost welded construction as the welder who is completing the joints, since it is equally as important to get good fit-up as it is to use proper low-cost welding procedures. Poor fit-up means more weld metal must be added in addition to the fact that lower welding currents and smaller electrodes must be used to avoid burning through. This is shown in Fig. 17. To get some idea of the increased cost of welding due to poor fit-up let us consider a $^1/_4$-in. fillet weld (Fig. 18). With proper fit-up and assuming an over-all speed of 10 ft. per hour, this joint would cost 27¢ per foot for labor, electrode and power. With a $^1/_{16}$-in. gap this

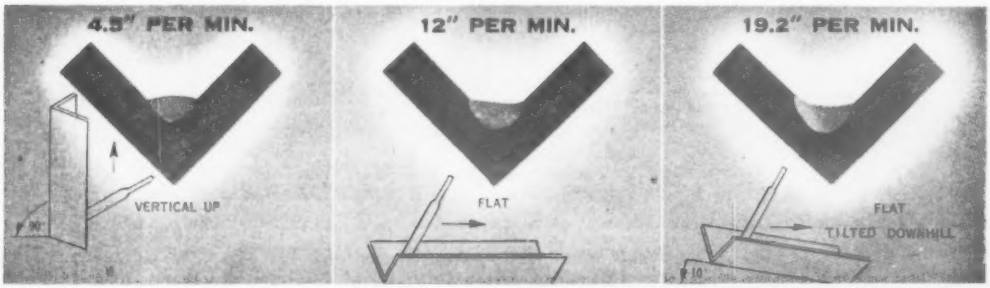


Fig. 15 Effect of position on speed of welding fillet welds in place $^1/_4$ in. or thinner

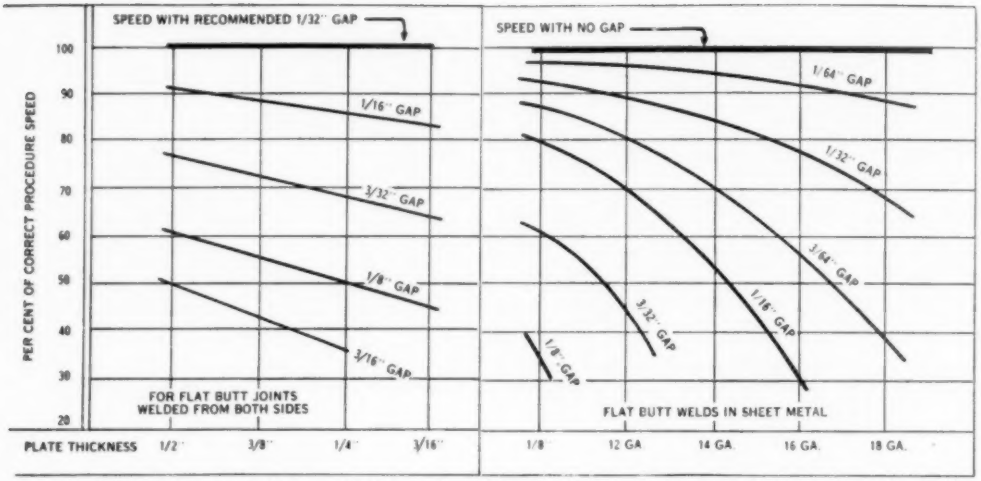


Fig. 17 Effect of fit-up on welding speed of down-hand butt joint (square or grooved) of various plate thicknesses

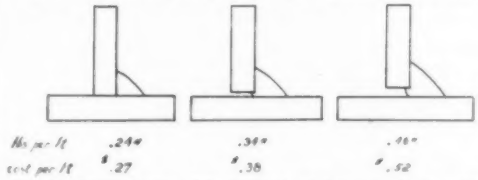


Fig. 18 Poor fit-up adds to cost and amount of weld metal needed

cost would increase to 38.2¢ per foot because of the additional weld metal required. The joint with a $^1/_8$-in. gap would cost 51.7¢ per foot. Actually this increase in welding cost due to poor fit-up is greater than we have shown because less welding current would be used to keep from burning through on the joint having gaps.

In Fig. 19 we have three examples of poor fit-up. The first is a square edge butt joint of $^3/_8$-in. plate in which the welding speed is reduced from 9 in. per minute to 2.5 in. per minute because of a $^3/_{16}$-in. gap instead of $^1/_{32}$. This means a cost of 3.6 times greater. The second example is a $^1/_2$-in. plate with beveled edges butt welded with a $^1/_{32}$-in. gap and also a $^3/_{16}$-in. gap. The $^3/_{16}$-in. gap caused a reduction of speed from 3.8 in per minute to 1.9 per minute and doubled the cost. The third case is a $^5/_{16}$-in. fillet; here a $^3/_{16}$-in. gap reduced the speed from 12 in. per minute to 3 in. per minute and made the cost four times as great.

Shearing in most cases is the fastest method of making straight cuts; for irregular cuts flame cutting by torch is recommended. When the number of pieces warrant, it is possible to use blanking dies for cutting. This is advisable since close tolerances can be held without difficulty. Many times steel castings or forgings

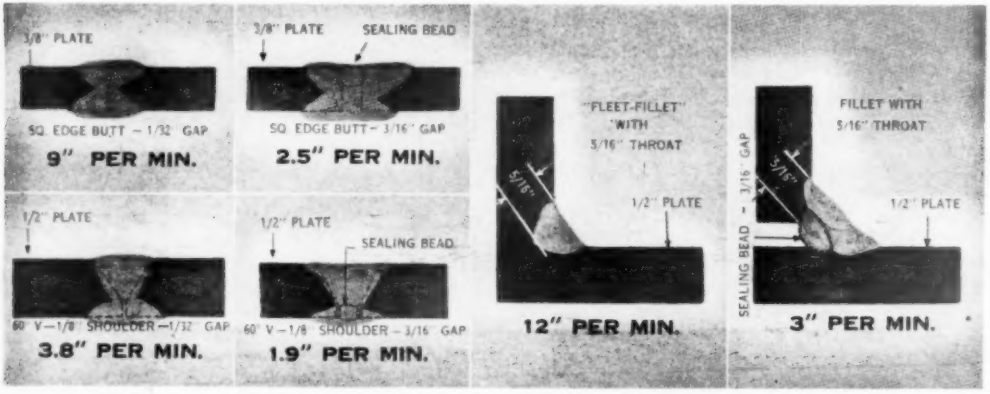
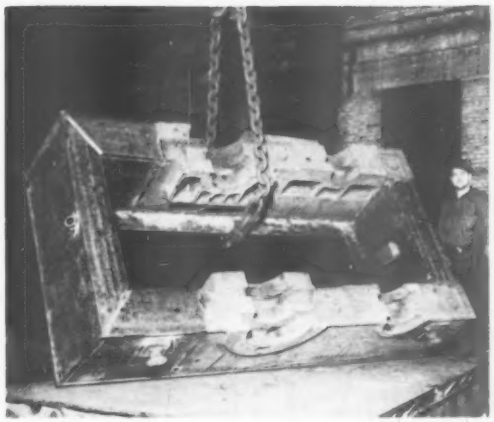


Fig. 19 Effect of fit-up on welding speed

(Left) Butt welds in $^1/_2$-in. plate, showing effect of gap on welding speed. Note: Sealing bead and added electrode metal required with wider gap

(Right) Fillet welds in $^1/_2$-in. plate, showing effect of gap on welding speed. Note: Sealing bead and added electrode metal required with wider gap



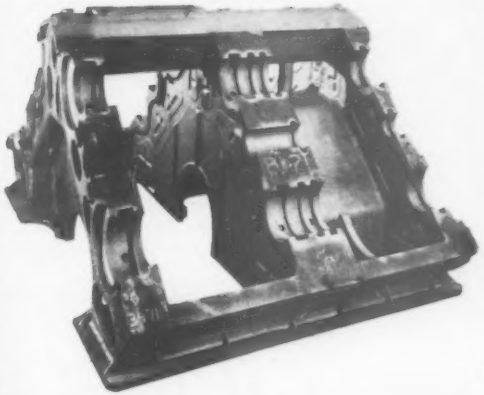


Fig. 20 The above figure shows the use of steel castings in weldments. Photo, courtesy Falk Corporation of Milwaukee, Wis.

may be welded into structures where the design is more intricate or perhaps a round corner is required for appearance. In a similar manner formed heads and stampings may be used in a welded structure to advantage (Fig. 20).

The use of subassemblies in large structures cannot be overemphasized. If the design is intricate the smaller subassemblies may be stress relieved before final welding into the completed structure. In the same manner subassemblies may be finished machined. Any complete compartments within them may be tested, thus eliminating final testing in the completed structure which would be more trouble. These subassemblies may be inspected and repaired before being welded into the final structure. It is obvious that there is greater access for welding, with these smaller subassemblies than the larger completed structure.

Much of this welding mentioned is being done with automatic heads. The advantages with the submerged arc process are many—much faster welding, almost no distortion and the welds are so smooth that no grinding is necessary even for the most exacting fabricator. Because of the much higher welding currents used, greater penetration can be obtained.

Figure 21 shows a base of which 80% was welded

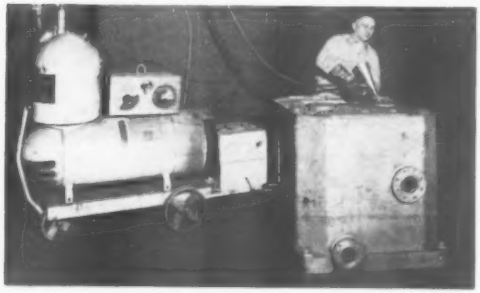


Fig. 21 Automatic welding proves more economical than hand welding

Table 2

	Hand welding			*Submerged arc*		
	Passes	*Amp.*	*Ft./hr.*	*Passes*	*Amp.*	*Ft./hr.*
10-gage butt	1	180	120	1	650	500
1/8-in. butt	3	350	19	2	800	68
1-in. butt	8	350	6	2	1000	32
10-gage edge	1	160	200	1	600	500
10-gage lap	1	200	90	1	750	400
1/8-in. lap	1	375	50	1	625	180

with the semiautomatic resulting in a 75% saving over hand welding. In Table 2 are shown currents and speeds for a few typical joints for both hand welding and the submerged arc automatic process. The savings are evident.

In conclusion we offer this program to aid in reducing the cost of welding. Since the problem is not contained wholly within the welding department, it is necessary that the other departments such as engineering, tooling, production control, inspection, time study, layout, burning, fabrication, etc., be brought in to bear some of this responsibility. It is necessary that the engineering department design joints which will give minimum costs through minimum weld metal, joints which are accessible for welding, etc. Proper jigs and fixtures should be used to aid in aligning parts and positioning for welding so that maximum current might be used. The burning and shearing department must hold dimensions of pieces close enough so that the fabrication department with proper care can produce joints with good fit-up. Welding supervision must hold proper welding size without excessive overwelding; see that maximum current, and electrode size is used for best welding; and avoid such faults as undercut, overlap, lack of penetration, excessive spatter, cracked welds, etc.

Some plants have found it difficult to get a good welding engineer who is qualified both as a welder and an engineer. Good men with this combination are not plentiful. To solve this problem they pick two men, one from the engineering department and the other a

good welder from the welding department. These men work together as a team on all problems pertaining to welding. The engineer passes on all things such as design and strength of joints while the welder works on procedures. You can be sure that any problem which passes through this combination will be sound from both the engineering standpoint and practical welding standpoint. Soon you will find new jobs being redesigned for welding with reduced cost and new procedures which will give further savings.

To sum it up, reduced welding costs are made by:

1. Using design with minimum weld metal.
2. Using good fit-up.
5. Positioning welds for flat welding in heavy work or vertical down for sheet metal.
4. Using maximum current and large electrodes.
5. Marking size of welds on work to avoid making too heavy welds.
6. Using good procedures to prevent undercutting or excessive spatter.

Evolution of the Welded Box Car

Evolution of an all-welded box car. Detailed description of welding roof, ends, sides and underframe

by L. E. Grant

THERE are approximately one and three-quarter million freight cars owned in the United States at the present time. Probably less than 30% have any welded joints and certainly a much smaller percentage are all-welded cars. The first welded freight cars were looked upon with some distrust. Car yards were not in general equipped with welding machines, and hence were unable to repair the welded cars except by substituting rivets for welds. There was considerable reluctance on the part of many carmen to believe that welded joints could endure the severe loads and vibrations encountered in frieght service. The welded cars generally gained acceptance and finally were recognized as being not only durable but in general very satisfactory equipment. In recent years there has been a rapid swing to welding in preference to riveting.

The Milwaukee Road was a pioneer in building welded cars and in the 13 yrs. since the first cars were built in the railroad's shops, nearly 30,000 welded cars have been constructed, in addition to several hundred passenger train cars including the well-known Hiawatha trains. The evolution of the welded type of car is an interesting example of industrial development, paralleling in many respects similar changes from riveting to welding in other fields. The Milwaukee Road was in an enviable position to undertake the construction of welded cars as the shops had never built riveted cars and there was not so much to unlearn or prejudices to overcome. Riveting equipment, of course, was available and in use for repair work but only a limited amount of welding equipment was on hand. It was necessary to acquire the equipment for both arc and resistance welding.

There are a number of reasons for favoring the welding method for building freight cars. In general the welded car can be assembled at a lower cost than a riveted car of the same type once a shop has been suitably equipped. Welding makes it possible to eliminate many of the lap joints that form pockets for collecting corrosive agents such as dirt and moisture. This is especially important in coal cars. It has happened that the introduction of welding and the low-alloy high-tensile steels have coincided so that many of the welded cars have been built of the high-tensile steel. The increased corrosion resistance of this steel, as compared to carbon or copper bearing steel, has naturally helped to lengthen the life of the welded car and also to decrease the overall weight. It is important to save dead weight and railroad officials have made various estimates, up to 10 cents per pound, as to how much they can afford to pay for each pound of weight saved in a freight car. Welding and the use of the high-tensile steels have brought the weight down to the point where the braking problem is now a factor in determining the minimum car weight permissible in some classes of cars that carry heavy pay loads.

It is important to emphasize here that it is not possible to change from a riveted to a welded design for a car by simply writing on the blue print, "Weld all joints." It cannot be done and to try is to invite not

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Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

only economic failure, but other failures as well. Combining both welding and riveting in a single car, whether it be a new car or for repairs, requires care and good engineering. The inherent rigidity of the welded joints may cause an unforseen distribution of stress and lead to failure of the welds.

In order to explain the evolution of the welded car a box car will be used as an example because it incorporates all the basic elements used in a freight car. It will be necessary in the discussion to refer to the methods used in building a riveted car so that the change from one type of construction to another will be clear. To those not familiar with the various parts of a car and their names, this arrangement may help to clarify the details that are described. Construction of the roof will be described first, this being the simplest part of the car. A description of the procedure for making the ends, sides and finally the underframe will follow.

THE ROOF

The roof consists of a number of 14-gage galvanized steel sheets 40 in. wide, slightly longer than the car is wide because of the pitch to the roof. A number of methods were used in the riveted car to make a tight joint between adjoining sheets, but a typical method was to flange up the edges, place a cap over two adjoining flanges and rivet the assembly together as shown in Fig. 1. Another method was to rivet a cover strip over a butt joint between two sheets. In both methods ends of the sheet were flanged down over the side plate or upper section of the car side and riveted to it, as shown in Fig. 1 (*A*).

In changing the roof to a welded design a modification of the method shown in Fig. 1 was adopted, the cap and rivets being omitted. The flanged up edges were hammered together and joined by an edge weld. The entire roof was assembled in a jig so that all the joints could be welded with the workmen standing on the floor. Brackets to hold the running boards were welded to the roof and the interior lining of plywood was installed before the roof was placed on the car. Figure 2 shows such a roof completed and ready to be lifted by a crane and set in place on the car. Flanges at the edge of the roof extended down over the top plate of the side

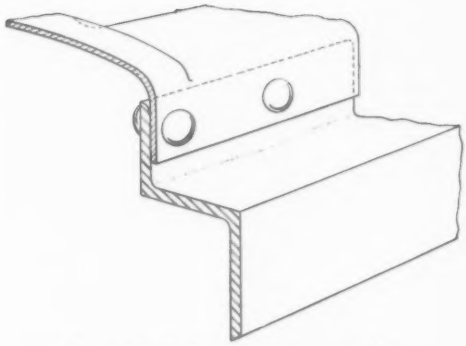

Fig. 1 (A) Roof to top plate connection

and were plug welded through rivet holes. Later these rivet holes were omitted and the edge of the flange was fillet welded to the top plate.

This type of roof was difficult to install. It had to be fitted down over the car while still suspended from the crane and a considerable amount of juggling as well as some hammering and cutting was necessary to finally get it in position. To remedy this an offset was made in the top plate to form a shoulder for the roof to rest on. Then an auxiliary angle, bent to conform to the flanged edge of the roof sheet, was added in the roof jig. This angle was welded in place with fillet welds at the edge of the roof sheets just as had previously been done on the car side. When this type of roof was placed on the car the horizontal leg of the auxiliary roof angle rested on the horizontal section formed in the top plate. Sometimes it was necessary to hammer the contacting leg of the angle down so that a good weld could be made. But the change greatly simplified fitting the roof in place and incidentally increased the capacity of the car. A horizontal fillet weld, entirely around the roof, was required to complete the job. A continuous bead was necessary to make the joint water tight. At first only flat sheets were used in making the roof but later a roof sheet with pressed stiffening panels was used for additional stiffness.

The edge joint of the roof sections was welded with a $^1/_8$-in. Class E-6010 electrode. The weld was started

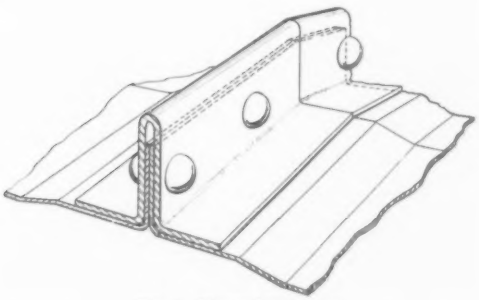

Fig. 1 Riveted roof joint

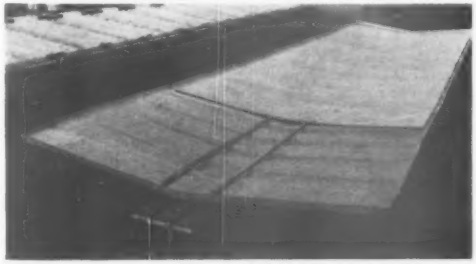

Fig. 2 Roof completed, ready to install on car

at the center and carried toward the outer edges, welding being slightly downhill. It has been recommended that this joint, at least, be made with a silicon-bronze electrode by either carbon-arc or metal-arc welding to improve the corrosion resistance of the weld. It is true that some of the zinc burns off the side of the flanges, just below the weld bead, but after 12 yr. of service no evidence has been found of significant corrosion of either the joints or the sheet adjacent to the joint and hence the higher cost of welding with a nonferrous rod cannot be justified.

ENDS

In a riveted car the ends consist of two corrugated sections; the upper being lapped over the lower and riveted to it. Ordinarily the top sheet is lighter in gage than the bottom section because less strength is required near the top. The ends are flanged at the sides to form the corner of the car and these flanges are riveted to the corner posts. The lower edge of the end laps over a transverse end still and is riveted to it.

Changing this to a welded design was fairly simple. The overlap in the center was omitted as well as the rivet holes, but the difference in thickness between the upper and lower halves was maintained—the top half being $^{5}/_{32}$ in. and the lower half $^{3}/_{16}$ in., both of low-alloy high-tensile steel. The two halves were assembled in a jig and the butt joint welded with an E-6010 electrode as shown in Fig. 3. The completed end with ladder and lumber door is shown in Fig. 4. Later, when a wide door post or pan-type post was adopted, similar wide end posts were also adopted and these were simply welded into place in the jig shown in Fig. 3 making the pans a part of the end assembly when it was ready for application to the car. There was no end sill used in this construction, but the lower edge of the bottom sheet was flanged to form a substitute for the end sill. This flange was welded only at the point where it rested on the center sill and to the side sills. A wood lining was also applied in the final steps of making the ends.

Fig. 3 Assembly of ends

Fig. 4 Completed end

Some of the early welded cars were found to exhibit a tendency for the ends to bulge especially in the bottom half. This was improved to a considerable extent by changing the depth of the corrugations from 3 to 4 in. (The completed car with the wide or pan-type end and door posts is shown in Fig. 5.)

SIDES

Two types of sides are commonly used in riveted cars. There is usually an angle or a pair of angles forming the top plate and a channel or angle at the bottom to form a side sill. These are connected by vertical posts and diagonal braces riveted to the top plate and bottom side sill. One type of car has wood sides bolted to this framework; the other type of car has steel sheets, ordinarily with a vertical lap joint, riveted to the framework.

In our first attempt to make a welded steel side the sheets were welded to the conventional posts and braces without changing the riveted connections at the top

Fig. 5 Welded box car

and bottom. Butt joints between the sheets were welded at the posts. This construction was very unsatisfactory from the standpoint of both appearance and weight.

The next step was to fabricate an all-welded steel side with Zee-bar pressings for posts and three rows of wide flanged U's for horizontal stiffeners, spot welded to the inside of the sheets. The sheets were arc welded at the top and bottom but were spot welded to the posts. The posts were attached to the side sill and to the top plate principally by fillet welds. No braces were used in this construction and butt joints between the sheets were arc welded at the posts as before. The sheets were 14-gage low-alloy high-tensile steel. This type of side was used for a time but was too heavy, showed undesirable warping during construction and required too many man hours to make.

The objection to this type of side led to the modification of it shown in Fig. 6 which shows the interior. It will be noted that the side is complete with the door as erected. Portal bracing is shown at the top and bottom of the door and it will also be noted that the door posts are essentially the same as the other posts. This construction was unsatisfactory as the posts were not strong enough, even with the portal bracing, especially on the automobile type of car with double width side doors. Later the door posts were changed to a wide-pan-shaped section, as mentioned above, to provide additional strength in the doorway. These wide door posts (and end posts) proved beneficial in other ways. Their use resulted in shortening the side sheets and this made them easier to handle in the shop with the equipment then available. They also strengthened the sides so that it was possible to hang the doors while the side was flat in the fabricating jig. Previously the doors had to be hung on the car after erection was completed. It should be noted in Fig. 6 that the side sill channel was attached to the underframe of the car and did not form a portion of the side proper. An additional section, called a side sill upper element, was used to connect the bottom sheet to the side-sill channel. This element also served as a support for the ends of the floor boards.

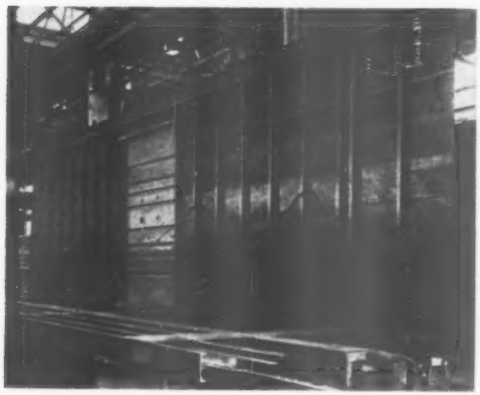

Fig. 6 Box car side with pressed horizontal stiffening ribs

When the completed sides were assembled on the underframe the upper element was pulled down into place against the side-sill channel with bolts. Later the bolts were removed and replaced with rivets. This method of construction was not satisfactory because of cracks that ultimately developed between the rivets. The rivet holes in both the upper element and the side sill were omitted and slots were cut in the element so that it could be welded to the top flange of the side sill channel. A few holes were required so that bolts could be used to draw the side down into place on the side sill. This method had a drawback in that it was very difficult at times to bring the upper element down into proper contact for good welding. The side was very stiff in a vertical direction and as the channel was already welded to the underframe it could not be moved very much. To correct this condition the slots were omitted from the upper element and the side-sill channel was attached to the upper element by fillet welds on both sides. In this method of construction the side-sill channel became a part of the side assembly instead of part of the underframe. The channel was later welded to the underframe when the sides were put in place.

The first welded car had flat side sheets as mentioned above but it was difficult to produce cars without objectionable buckles. Even the U-shaped stiffeners did not help appreciably in reducing the buckles. Experiments were made to determine if some horizontal stiffeners parallel to the length of the sheet could be pressed into the sheets and thus increase the ease of making better appearing sides and at the same time reduce the weight and increase the strength. The plans were to construct the sides by spot-welding methods. A comprehensive account of the tests that were conducted, with the above in mind, will be found in a paper by Green and Drinka before the A.S.M.E.*

This type of car side was constructed of 15-gage sheets (0.070 in.) of low-alloy high-tensile steel, and $^1/_8$-in. thick Z-bar posts of the same material. The door and end pans were $^5/_{32}$ in. thick, also low-alloy high-tensile steel. Although comparatively light in weight these sides had surprisingly high strength. The critical shearing load for a flat 15-gage sheet with $^1/_8$-in. posts, spaced approximately 2 ft. 4 in., is in the neighborhood of 9000 lb. When six horizontal bumps were pressed into the sheets, with the sheets overlapping at the bumps, the post spacing remaining the same, the critical shearing load became about 40,000 lb. This is a four-fold increase in strength at the expense of putting in the bumps. Even this load is not the maximum that can be carried.

The size and shape of the bump greatly influences the strength it imparts. Figure 7 shows the style and dimensions of the bump finally selected after many tests were made. Although the sheets may seem thin for a 50-ton capacity box car, actually they may not be stressed beyond 5000 psi. in service. Much thinner sheets could

* Green, V., and Drinka, J., *A.S.M.E.*, 561 (October 1945).

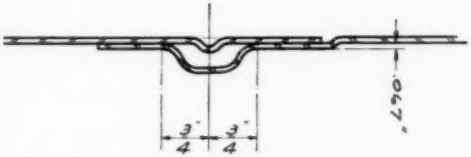

Fig. 7 Details of horizontal stiffener (bump) pressed in car side sheets

be used and yet be adequate for strength, but slight corrosion would be very serious.

The side sheet was lapped over and welded to the end pan after the sides and ends had been erected. This weld was made vertical down with an E-6013 sheet metal type of electrode. This joint must be sound and free of pinholes to avoid the possibility of water getting in behind the thin sheet and causing serious corrosion. The weld between the sheet and the door pan was made in the flat position in the side jig where the door, door header, spark strip, route boards, etc., were installed.

Figure 8 is a general view of the assembly line for car sides showing the specially constructed spot welders for spot welding the sheets and posts. This latter welder gave a good deal of trouble due to the excessively deep throat involved in welding a car side. This was remedied later by the use of phase-shifting and current-controlling equipment. A 40-ft. 6-in. box car required approximately 6000 spot welds.

UNDERFRAMES

The underframe of a box car shown in Fig. 9 is the

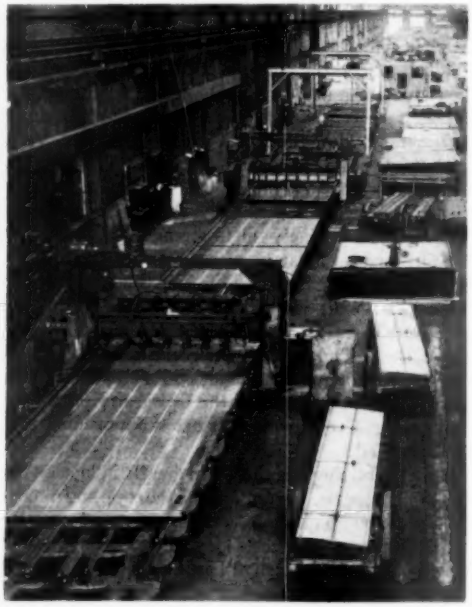

Fig. 8 Assembly line for fabrication of car sides by spot welding

Fig. 9 Underframe of a welded box car

longitudinal strength member and serves as a column to take the end loads and shocks of switching and train operation. In recent years two Z-bars welded together have been used for the center sill of cars of the riveted type of construction. Prior to the adoption of the Z-bars, the center sills consisted of I-beams, channels, tees or plates with angles riveted to them. When channels, tees or I-beams were used, they were fastened together with riveted cover plates. In addition to the center sill, the longitudinal member which is the backbone of the underframe, there are three types of cross members called bolsters, cross bearers and cross ties, in decreasing order of strength. Lighter, longitudinal members formed into Zees are used to support the floor boards. The channel-type side sills previously mentioned form the outer longitudinal members of the car underframe.

Two important members of the center sills of the riveted cars are the center filler and draft attachment or striker. Both are steel castings riveted to the center sill. The center filler carries the center plate and pin which are the main attachments between the car body and the truck. One of these is located near each end of the center sill. The striker is riveted to the end of the center sill and in combination with the draft gear and draft lugs provides a foundation from which the coupler and yoke can draw the train and take buffing shocks of one kind or another. These two items will be discussed further later on in this paper.

In building a welded underframe, the two Z-bars welded together along the adjoining edges of two of the flanges were used the same as in a riveted car, although these Zees are not an ideal section for this purpose. In many car plants the Z-bar is joined with a continuous weld made by the submerged-arc process from one side only, making a 100% penetration weld though this much welding is not required for adequate strength. In the Milwaukee Shops they were welded manually in a special jig, using an E-6020 electrode, 1/4 in. diameter. They were welded on both sides from each end to beyond the bolsters and then skip welded between

Fig. 10 Cast steel center filler with openings for welding

Fig. 11 Fabricated center filler with cast center plate

these two continuous welds, one weld being inside and the adjacent weld outside.

After the Z-bars had been welded, the center fillers and draft lugs were put in place. In the first welded cars these were castings and were riveted because it was not then known how to weld them successfully. Later castings were made with slots to provide a place for fillet welds. The type of casting used is shown in Fig. 10. All the welding was done on the inside before the center sill was turned over in the erection jig.

During the war when castings were hard to get these parts were fabricated by welding a combination center plate and spider casting to some channel-shaped pieces called cheek plates formed from plate. Instead of making slots in the cheek plates for welding, the slots were cut in the center sill and the welds made from the outside. This was somewhat easier to do than welding the slots in the castings and proved satisfactory. Later the center filler was changed entirely to a weldment shown in Fig. 11. The spider portion (actually the support for the center plate) was made from plate sections welded together. The spider was then welded to channels, which happened to be easier to get than plate, as well as being lighter in weight. This method saved the forming operations involved in making the cheek plates mentioned above. The center plate was forged from a flat disk and welded to the above assembly. The entire welded center filler was then welded to the center sill as before through slots cut in the web of the center sill. Some fillet welds were also used around the edges of the section.

This center filler development has been of considerable interest because of the step-wise progress in changing over to an entirely welded method of construction and the uncertainty and concern over the feasibility of it. On the first castings many more inches of welding than necessary were used because the magnitude of the impact stresses to which this part would be subjected could not be calculated. Later the amount of welding was reduced and finally, when the change to the fabricated center filler was made, the welding footage was reduced still further. That the decision to reduce the amount of welding was sound has been borne out by the absence of failures in either the center filler weldment or the welds attaching it to the center sill. Some failures of the combination of cast center plate and filler have been experienced but these were due chiefly to improper design and it was 8 to 10 yr. before these failures became a significant factor in maintenance. The fabricated support for the center plate was not made strong enough and failed vertically. The improved design has shown no signs of failure.

The main transverse strength members of the underframe are the bolsters. These are located near each end of the car. In the riveted cars these members consisted of two pan-shaped pieces formed from a plate. They were riveted to the center sill and to the side sill. Riveted cover plates, top and bottom, completed the unit as shown in Fig. 12.

It was a fairly simple matter to make a bolster by shearing two web plates to take the place of the pans and make a sturdy box section by welding them to separate top and bottom cover plates in a jig. The assembly was then welded to the center sill and side sill. Unfortunately the results were not entirely satisfactory. The structure had ample strength for a bolster but there was considerable tendency for them to break off at the center sill.

To correct the condition, horizontal gusset plates were applied between the bolster and center sill at all four corners. This did not prove satisfactory so a cover

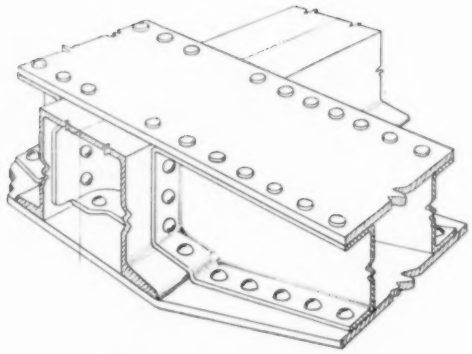

Fig. 12 Riveted type of bolster

plate over the center sill extending out over the bolsters was tried. This helped but did not eliminate failures. Finally the bolster was redesigned to make the top cover plate continuous over the center sill and at the same time the web plates were changed so that they had a wider spread at the center sill than at the side sill. The load of course is much more severe at the center sill connection than at the side sill. Improvements in the jigs and in cutting the web plates provided better fitting joints that could be welded satisfactorily. This type of construction has been in use for about 2 yr. and at the present time appears to be reasonably satisfactory but further improvement is anticipated by proposed changes.

The cross bearers are the horizontal cross members at each side of the doorway. In the riveted car they were much like the individual pans of the bolsters and were riveted to the center sill and side sill. The cover plate was not continuous on these.

The welded cross bearers consist of a top and bottom cover plate with a single web-plate fillet welded to them. The top cover plate is not continuous on the cross bearers as the loading is not very heavy and shocks are minor. No trouble has been experienced with the cross bearers or cross ties failing.

The methods described herein are those used by the Milwaukee Road. Other railroads and car builders have used other welding methods to accomplish the same results. Some make extensive use of automatic submerged-arc welding and do not use spot welds. Various parts in the underframe are made by different methods but the net results are very similar. The type of shop equipment available, the character of the shop buildings and the ideas of the car designer and shop personnel all play a part in how the cars will be built. However if the welded car is properly engineered and well constructed it will give good service regardless of how the details are worked out.

Welding Metallurgy—Iron and Steel

Due to the large number of papers for presentation at the A.W.S. Annual Meeting, we regret that our September and October issues cannot include the customary chapters of the new edition of "Welding Metallurgy," by Henry, Claussen and Linnert. The next chapter will appear in our November issue

FORTHCOMING installments of the Welding Metallurgy series will include the subjects of "Welding Methods and Processes," "Structures of Metals," "Effects of Alloying Elements," "Welding Plain-Carbon and Low-Alloy Steels" and "Welding Stainless and Heat-Resisting Steels."

"How does chromium assist iron-base alloys in resisting attack when exposed to various corrosive or oxidizing media?" The answer is clearly given in Chapter 14 on page 387 of *Welding Metallurgy* wherein it is stated—"Briefly, the resistance to attack is brought about by the great affinity which chromium has for oxygen and the passive or protected surface that is formed when the chromium-containing metal is supplied with oxygen under the proper conditions. The exact nature of this surface protection has yet to be learned. Several hypotheses on this matter will be discussed later, but for the present we can say that chromium-containing steels protect themselves from corrosion by forming an impervious film of oxide on the surface."

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Heliarc Welding of Aluminum Alloys

Heliarc Welds properly made are able to withstand severe cold-working operations. Suitable techniques for welding aluminum alloys are indicated

By Francis H. Stevenson

MUCH has been said during very recent years regarding the success enjoyed by the relatively new welding process called "Heliarc" for the welding of practically all metals including the once hard to weld aluminum.

The author in this article will attempt to reveal the importance of this process in the metal-spinning industry where the weld metal must withstand the same torturing cold-working operation as the base metal.

To begin with some of the fundamental advantages of the process are:

1. Flux is not required, therefore flux corrosion is eliminated.
2. Cleaning and grinding costs are at a minimum.
3. Welds can be made with or without filler rod.
4. Lack of sparks, smoke and spatter lends for ease of operation and lower rod cost.
5. Only fusion welding process, by which nonpositioned welds can be made.

Welds made by this process consistently pass X-ray examination and also show high mechanical properties.

We can divide the aluminum alloys into two groups. First, the nonheat treatable which consist of 2S, 3S and 52S. Second, the heat treatable which consist of 53S, 61S and 63S.

We will now discuss the welding of the first group of alloys. The 2S or commercially pure aluminum is the most weldable. However, the alloy known as 3S has very similar welding characteristics. Therefore we will discuss these two materials together. They both can be welded manually or mechanically either with or without filler material. The type of filler material used for 2S is the 2S drawn wire. The filler material for 3S can be 2S wire, strips of the 3S alloy or the 5% silicon alloy. However, recent indications are that welds made with the 5% silicon rod are subject to intergranular corrosion after a period of service. The type of filler metal used with the 3S depends on the part being welded and what happens to the part in operation. If the part is to be spun after welding we have found it advisable to use strip stock for our filler metal. This, of course, applying when the parts are welded manually. If the parts are welded mechanically no filler metal is used. However,

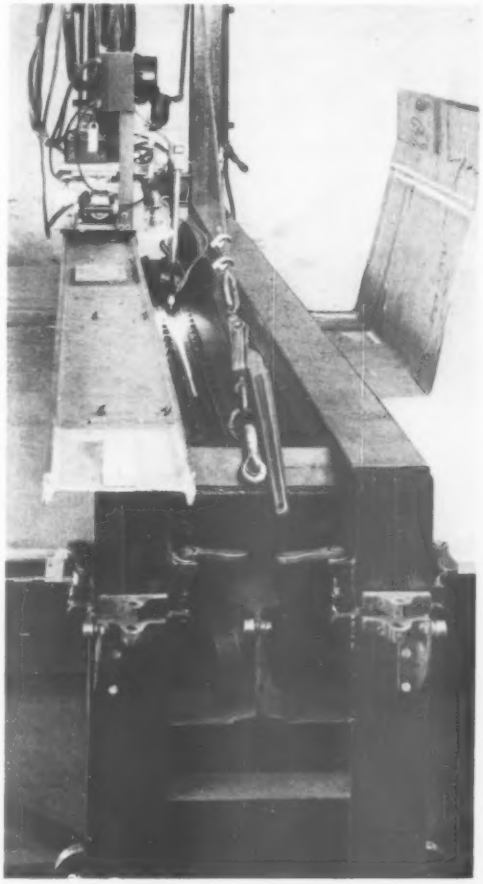

Fig. 1 Welding Fixture

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Scheduled for presentation at Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

there are some parts welded mechanically which require filler metal and in that case 28 wire is used. We will now attempt to describe the automatic welding of parts without the use of filler metal. To date our experience has dictated to us that it is essential that we keep the weld area thickness comparable to base metal thickness in order to avoid notch effect and the resulting failure in spinning operations.

Preparatory to welding the parts are cut, rolled (if tube or cone) and the edges given a steel wooling sufficient to remove surface oxides. Prior to inserting in the welding fixture the parts are wiped with a cleaning solution to preclude the oil and grease that may be picked up from handling. Figure 1 shows welding fixture used by the Thomas D. McHale Mfg. Co. of Los Angeles. We regret that security regulations have prevented our incorporating photographs covering fixtures in use at Modern.

The particular welding condition used for welding the 0.064-in. material for spinning is as follows:

Copper back-up bar grooved 0.007 x 1/4 which provided most suitable drop through.
3/16-in. tungsten electrode.
No. 10 gas cup having 5/8-in I.D. opening.
0.255 electrode spacing from work.
1/8 in. of electrode protruding from gas cup.
10 l. argon flow.
38 in. per minute travel speed.
75-lb. air pressure on copper hold-down shoes.

Table 1 gives data relative to mechanical welding.

Table 1—Mechanical Welding of Aluminum Using Alternating Current, High-Frequency Grooved Steel, Copper or Aluminum Back-up

Thickness in.	*Type of weld*	*Welding current amp., flat position*	*Welding speed, ipm.*	*Welding rod size, in.*	*Argon flow L./- min.*	*Cu. ft./hr.*
0.025	30° flange	45	28	None	5	11
0.040	30° flange	85	26	None	7	15
0.040*	Edge	255	300	None	10	21
0.040†	Edge	230	150	None	10	21
0.050	30° flange	135	24	None	7	15
0.064	30° flange	130	48	None	8	17
0.081	Butt	190	26	None	8	17
0.090	Butt	190	18	None	8	17
0.090	Butt	235	48	None	8	17
0.125	Butt	285	26	None	8	17
0.125	Butt	315	48	None	8	17

* For 28 and 38 aluminum.
† For 528 aluminum.

Figure 2 shows the part welded and trimmed ready for spinning; Fig. 3, part partially spun; Fig. 4, part completely spun. From these figures can be seen the amount of working the weld itself must take and why we have to have welds which will take the same amount of forming as the parent metal.

The 52S alloy is somewhat more difficult to weld due mainly to its crack sensitivity, the cracks occurring either in the weld itself or in the material adjacent to the weld. The filler material used is the 5% silicon alloy. The melting point of this rod is lower than that of aluminum, consequently when the weld pool freezes the filler material solidifies last. Thus, the contraction

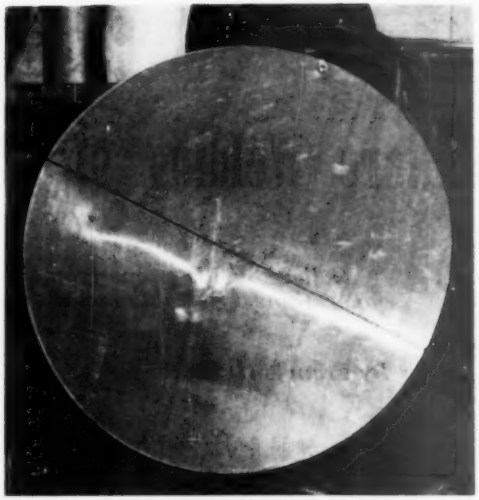

Fig. 2 Blank welded and trimmed ready for spinning

strains that are sometimes sufficient to cause cracking at the weld area are taken up in a manner somewhat similar to that which a casting riser provides molten metal to a solidifying casting. An example of welding 52S alloy is shown in Figs. 5 and 6. The broken half sections are positioned on the fixture shown in Fig. 5 where each side is longitudinally welded. In making these butt welds on the duct approximately 1/2 in. at the start of the weld 5% silicon rod is used and the balance of the joint is fused. The use of the 5% silicon alloy at the start is sufficient to stop any cracking which otherwise occurs. The flange is then welded to the duct as shown in Fig. 6 and on the edge weld the same type of rod is added where necessary. This weld is made from the inside against copper back-up plates which limits the amount of penetration.

In keeping with the practice of Heliarc welding the most common aluminum alloys we must also include

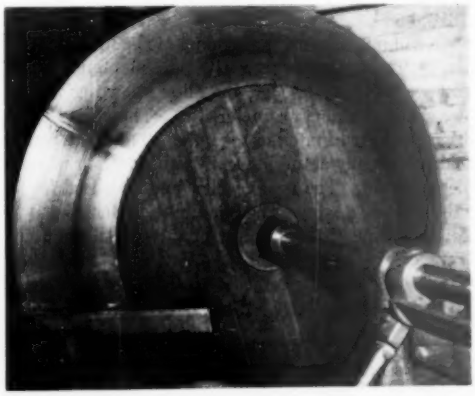

Fig. 3 Part partially spun showing weld at first contour

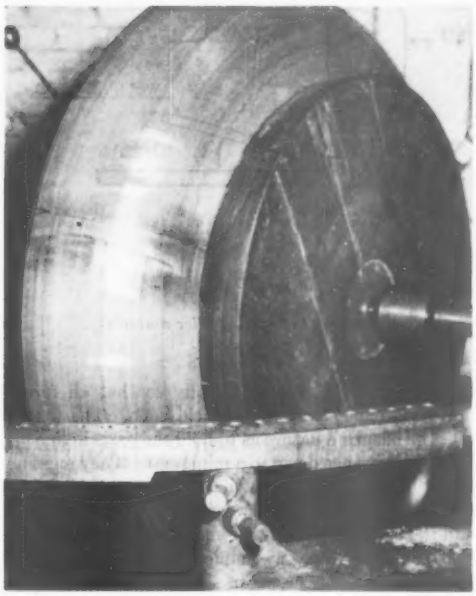

Fig. 4 Part completely spun showing weld

the heat-treatable group which are the 53S, 61S and 63S which are subject to the crack sensitivity, as is the 52S alloy. Therefore to date the best filler metal is the 5% silicon alloy.

To make clear the effect of welding on the heat-treated structures of these alloys and therefore on their strength it is necessary to describe some of their metallurgical characteristics. These alloys contain alloying constituents added to the aluminum base metal. By bringing the temperature to 970° F. and quenching in cold water, the constituents are so held in solution in the base metal that the mechanical properties are raised considerably. This process is known as the solution heat treatment, and the material is then designated as being in the T4 temper.

By subjecting the material to an additional heat treatment or aging—namely, holding the material at 320° F. for 18 hr.—the mechanical properties of these alloys can be raised further. The material is then designated as being in the T6 temper.

When annealed material ("O" temper) is welded, the heat does not lower the mechanical properties, as the cast material in the weld is stronger than the parent alloy.

This is not true of welding material in the T4 or T6 tempers. The welding heat lowers the strength of the heat-treated parent material in the area near the weld, although the metal is not fully annealed by the welding

Fig. 5 Welding fixture with parts in place ready for welding

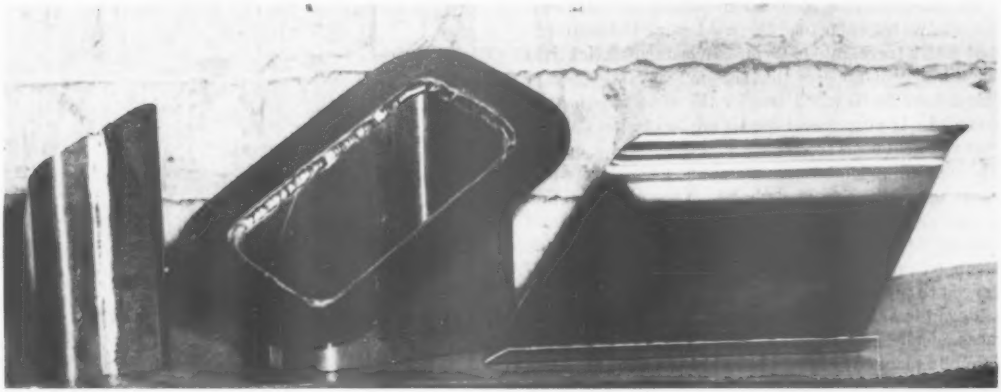

Fig. 6 Stack shown on left after welding. Completed duct in center. One half of stack shown on right

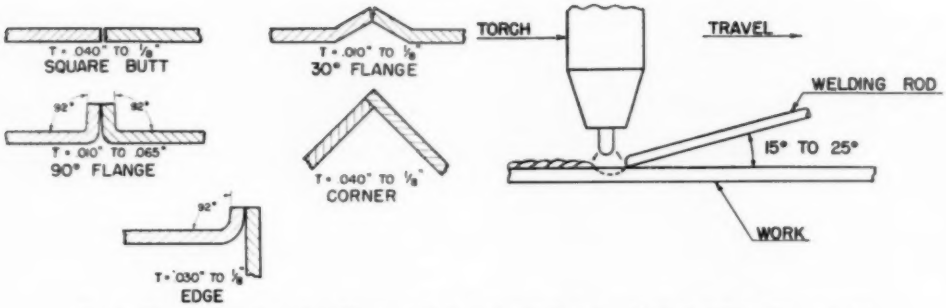

Fig. 7 Edge preparation when filler material is not added, and method of adding filler material

operation. Some air quenching occurs as the torch moves along the joint and the metal cools. The strength of the joint is, therefore, more than the strength of the annealed material but less than the strength of material in T4 or T6 tempers.

However, welds can be produced in these alloys with the weld-joint strengths greater than those obtainable with the common alloys, 2S and 3S. At the present time and with the filler materials which are now available, namely, 5% silicon and possibly the 716 alloy, we are still subject to crack sensitivity. However, we at Modern Metals are not only concerned with the manual welding of these alloys but also the mechanical welding. It is relatively easy to weld these alloys manually inasmuch as the majority of the joints call for the addition of filler material. However, to weld these alloys mechanically without filler material and then to have the parts spun presents quite a different problem. This is where the crack sensitivity shows up to its maximum. If you were to put a piece of flat stock in a mechanical welding fixture and just run a bead on it you will find that it will crack wide open just as fast as the torch is moving along and, when taken out of the fixture, you have two pieces instead of the original one. In our experiments to date we have been able to weld and spin these alloys with a marked degree of success.

On one particular application using 61S-W 0.064-in. material we were able to obtain welds using 150-amp. current with a travel rate of 23 in. In comparing this with the 3S alloy it appears that the amperes and speed of travel have to be much less for the same thickness of material. If your travel speeds are too fast it appears that the weld cools too rapidly and the cracking occurs. There is still much to learn about the mechanical welding of these alloys without the use of the filler material. We are experimenting with the mechanical welding and spinning of 61SO $^3/_8$-in material. However, our experiments on this thickness of plate are not sufficiently advanced to provide any data.

Regardless of the alloy welded whether manually or mechanically and what happens to it after welding a carefully planned welding procedure is necessary. This will include proper cleaning, adequate welding fixtures and sequence of welding. The cleaning can be done either chemically or mechanically so that all foreign matter is removed from the weld zone. The fixtures must be of such a nature as to hold the parts in line and keep the warpage down to a minimum. They must not hold the part so tight that it cannot move when contraction takes place. However, parts may be tacked and the clamps loosened before completing the weld.

If you are welding mechanically and using a fixture similar to the one shown in Fig. 1 and getting holes, skips, lack of penetration or rough welds one of several things may be the cause.

1. Parts may be dirty.
2. Air pressure on the clamps need adjustment.
3. Improper alignment between jig and center line of joint.
4. Improper electrode distance.
5. Air entrainment in the argon stream caused by loose connections, etc.
6. Shear cut on butting edges not straight.

If the part has several welds the sequence in which the welds are made must be such that the contraction is not all placed at one point but distributed as evenly as possible between the different welds.

Figure 7 shows edge preparation when no filler material is to be used and the method of adding filler ma-

Fig. 8 Irrigation pipe coupling. Small coupling welded, not beaded. Large coupling welded and beaded

Table 2

Thickness, in.	Type of weld	Amperes* Flat	Amperes* Vertical	Amperes* Overhead	Rod	Argon flow- L./min.	Argon flow- Cu. ft./hr.	Remarks
1/16	Butt	69	75 down	75	1/16	7	15	
1/16	Edge	63	58	58	None	7	15	
1/16	Corner	71	69 up	69	None	7	15	
1/16	Lap	981	58	69	None	7	15	
1/16	Fillet	69	92 down	75	1/16	7	15	
1/8	Butt	144	132 down	138	1/8	8	17	3/32-in. rod on overhead
1/8	Edge	132	135 down	140	None	8	17	3/32-in. rod on vertical
1/8	Corner	144	135 up	150	None	8	17	3/32-in. rod on vertical
1/8	Lap	161	144 up	92	None	8	17	3/32-in. rod on overhead
1/8	Fillet	161	135 down	161	1/16	8	17	
1/4	Butt	288	259	247	1/8	12	25	
1/4	Corner	288			3/16	12	25	
1/4	Lap	345	259 up	259	3/16	12	25	1/8-in. rod on vertical and overhead 2 passes
1/4	Fillet	316	247 up		3/16	12	25	1/8-in. rod on vertical
1/2	Butt	432	288	250	3/16	15	32	
1/2	Lap		345	316	1/4	15	32	3 passes
1/2	Pos. Lap	432			1/4	15	32	2 passes
1/2	Pos. Fil.	460	316		1/4	15	32	2 passes
3/4	Butt	500			1/4	15	32	5 passes-back-chip with "bull nose" chisel
1 1/4	Butt	500			1/4	15	32	8 passes-back-chip with "bull nose" chisel

* All current values are metered readings. Transformers designed for metal arc welding deliver about 15% more current than shown on their scale reading.

terial. This can be applied to manual as well as mechanical welding.

Table 2 gives data relative to manual welding of aluminum using alternating current and high-frequency welding current.

Figure 8 shows two irrigation pipe couplings. These are made in various sizes from 4 1/2 to 10 1/2 in. in diameter. The part on the left is 4 1/2 in. in diameter after welding and before beading. The part on the right is 10 1/2 in. in diameter after welding and beading. These parts are all welded manually using a square butt joint and no filler material.

If the reader has gained one usable point out of this paper the author feels that he has accomplished his purpose.

ACKNOWLEDGEMENTS

We express our sincere appreciation for the cooperation of the Thomas D. McHale Mfg. Co. for photograph used and to The Linde Air Products Co. for data supplied by their B. R. Russell.

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How to Weld Sheet Steel, Part II

Requirements and techniques for welding sheet steel by the inert-gas-shielded arc process

by K. H. Koopman and F. J. Pilia

THE welding of sheet steel by the oxyacetylene process was discussed in Part I of this article. In Part II the requirements and techniques for welding sheet steel by the inert-gas-shielded arc process are described.

Both manual and mechanized welds can be made with, or without, filler-metal additions when fully killed plain-carbon or low-alloy steel is used. Fully killed steel is deoxidized when it is made and weldments show no porosity with inert-gas-shielded arc welding. The type of joint design and the type of service to which the joint will be subjected should be the determining factors for the addition of filler rod to welds made in fully killed sheet steel. If filler rod is used, it is recommended that fully killed, low-alloy steel welding rod be used.

When rimmed steels are welded, there is a tendency toward porosity, because of gas reaction in the molten weld puddle. Rimmed steels are made with little or no addition of deoxidizing elements. As a result, the relatively unstable iron oxides are broken down when the steel base metal is heated to the welding temperature. The oxygen thus freed combines with carbon in the steel to form carbon monoxide gas. As the weld metal freezes, the gas is trapped in small bubbles which causes the

Fig. 1 A water-cooled inert-gas-shielded arc-welding torch—the Heliarc HW-4

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Table 1—Mechanical Welding Plain-Carbon and Low-Alloy Killed Steels

Thickness, in.	*D. C., straight polarity, amp.*	*Welding speed, in./min.*	*—Welding Rod*—* *Size, in.*	*Feed rate, in./min.*	*Argon flowmeter setting Liters/min*	*Cu. ft./hr*
0.035†	95	26	None	None	8	17
0.049†	118	26	None	None	8	17
0.060‡	130	26	None	None	8	17
0.060‡	200	30	1/16	17	8	17
0.089‡	160	20	None	None	8	17

* Use a fully killed, low-alloy steel welding rod.
† Low-alloy steels.
‡ S.A.E 1010 killed steel.

Fig. 2 Using the inert-gas-shielded arc-process to weld fenders for a well-known make of automobile

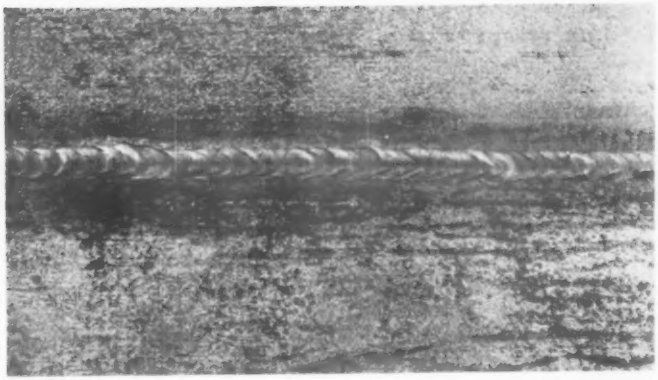

Fig. 3 An as-welded joint made in mild steel by manual inert-gas-shielded arc welding

Table 2—Manual Welding Plain-Carbon and Low-Alloy Killed Steels

Thickness, in.	*D. C., straight polarity, amp.*	*Welding rod size, in.* *	*Av. welding speed, in./min.*	*Argon flowmeter setting Liters/min.*	*Cu. ft./hr.*
0.035	100	1/16	12	8	17
0.049	100–125	1/16	12	8	17
0.060	100–140	1/16	12	8	17
0.089	140–170	1/16	12	8	17

* Use a fully killed, low-alloy steel welding rod.

weld to be porous. Gases such as oxygen, hydrogen and nitrogen which are dissolved in the metal, may also contribute to porosity.

To make sound welds in rimmed steels, it is recommended that fully killed, low-alloy steel welding rod be added to all joints. This type of welding rod contains extra ingredients which deoxidize the weld puddle and reduce porosity.

Jigs or proper holding fixtures are an essential part of the welding setup when the inert-gas-shielded arc-welding process is used for welding sheet steel. The fixtures serve several purposes. Hold-down bars and the backup bar must exert a firm pressure to prevent creeping of the parts being welded. These bars also carry away some of the heat from the sheet metal adjoining the weld, which helps to reduce the possibility of distortion. Close contact must be maintained between the sheet steel and the backup bar to insure proper penetration and prevent burning through the sheet.

Correct jigging requirements are as follows:

1. Backup and hold-down bars should preferably be made of copper or aluminum bronze.
2. Backup bars should have a shallow relief groove (about 0.010 to 0.015 in. deep) directly under the weld.
3. The width of the relief groove should be about 1/4 to 1/2 in. Hold-down bars should be spaced about 1/8 to 1/4 in. from the weld center line.
4. The bearing surface of hold-down bars should be 1 in. wide, adjacent to the weld. The bearing surfaces must be fitted accurately so that continuous contact is maintained between the sheet and backup bar. Variable contact will result in uneven penetration, burning through the sheet, or failure to penetrate.
5. Air or hydraulic cylinders or other dependable means should be used to operate the hold-down bars.
6. Construction should be rugged and hold-down pressure such that parts are not permitted to move during welding.

Current used for welding plain-carbon and low-alloy steel with this process is straight polarity, direct current. Alternating current is unsatisfactory because it produces more distortion, porosity and a tendency toward hot cracking in the weld, because of its lower rate of heat input.

Several requirements are essential in developing good welding technique and practices with the inert-gas-shielded arc process. They are as follows:

1. Wherever porosity is apt to create a problem, use a fully killed, low-alloy steel welding rod—in most cases, 1/16 in. in diameter.
2. Rate of welding should be quite rapid (see Tables 1 and 2).
3. Starting and stopping the arc is done on the fixture or jig bars.
4. When restarting the weld at the end of a previous weld, the overlap should be as short as possible. Dwelling too long at the start will tend to cause porosity.
5. If repairs are necessary, they should be made with a rapid U-motion by establishing an arc on the fixture. Place the end of the welding rod over the defect, move the arc out to the defective area of the weld, make the repair, then move back to the fixture to extinguish the arc.
6. Exclude air from the underside of the weld by maintaining a shallow relief groove in the backup bar.
7. Use 1/16-in. diameter welding rod for thickness up to 0.090-in. sheet, if filler metal is required.
8. Use a forehand welding technique with a straight line motion of the torch.
9. Welding of sheet steel can be done in the flat, vertical or overhead positions. Where gaps are encountered, repositioning the work from a flat to a slightly downhill position will make welding easier. The techniques and welding conditions are almost the same for all positions. Welding on a vertical joint usually progresses from top to bottom.

Improved Knurl Drive for Resistance Seam Welders

by R. K. Waldvogel

SEVERAL different types of roll drives are available for use on electric resistance seam-welding machines. These include gear drives and friction knurl drives or various combinations thereof. The present friction knurl drive design in most general use today consists of a solid cylindrical member with a knurled groove machined around its periphery. This groove serves the double purpose of rotating the welding wheel by friction and at the same time maintaining the correct contour of the wheel face. The views shown in Figs. 1 and 2 give the most widely accepted design for longitudinal and circular drives, respectively.

After some thought, it was decided that an improved type of knurl may be used to drive the upper wheel, lower wheel or both wheels as the occasion demands. This development is not original with the author, having first been observed on the wire feed mechanism of an automatic arc-welding machine; however, its application on seam welders is believed to be new. A brief explanation follows.

Fig. 1 Present style drive knurl adapted for longitudinal seam welding

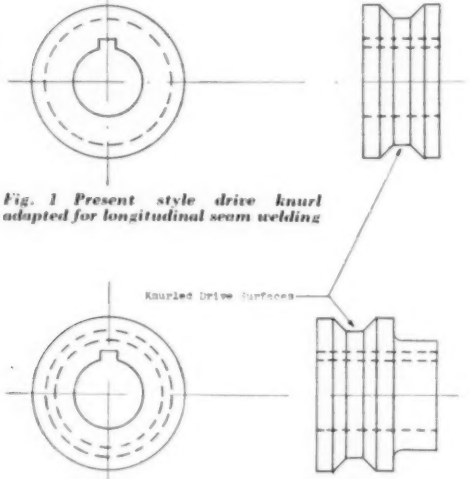

Fig. 2 Present style drive knurl adapted for circular seam welding

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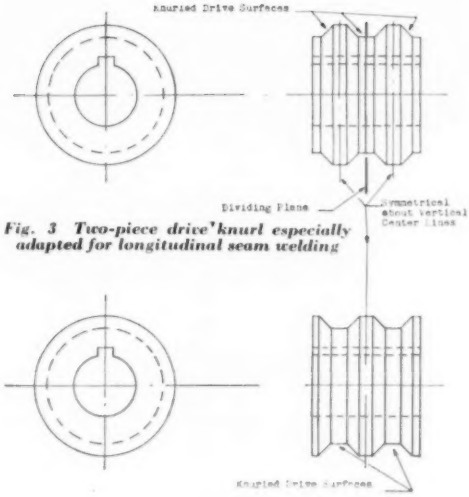


Fig. 3 Two-piece drive knurl especially adapted for longitudinal seam welding

Fig. 4 Solid drive knurl especially adapted for circular seam welding

NOTE: Assembly bolts not shown in above views

The essential feature of the improved knurl is to construct it of two exactly similar pieces, each symmetrical about its own vertical center line and assembled as shown in Fig. 3. The plane dividing the knurl into two parts may be shifted to either the right or the left, if necessary, with a subsequent redesign of the pieces. The drive roll as shown is placed on the welding machine and used until the knurled surface has lost its shape. Then all that need be done is to remove the knurl from its driveshaft, reverse the position of the two identical pieces and replace them on the welding machine. The new groove may be used until it, too, becomes worn. This knurl is especially adapted for longitudinal seam welding.

Another type of knurl is shown in Fig. 4. This is a solid knurl with two grooves machined in its outer surface. After one groove becomes worn, the drive wheel is reversed and the second groove is used. This knurl can best be used for circumferential seam welding. These sketches are intended to be suggestions only as it is realized that many variations in design are possible.

These designs will be placed in production at The Johnson Mfg. Co., as soon as practicable. Unfortunately costs savings cannot be given until work reports are available; although their use is expected to give the following advantages.

1. A decrease in machine shop costs as compared to making two separate knurls
2. A life expectancy double that of the old style knurl.

Flash Welding Unequal Thicknesses

Flash butt welding of a 3-in. diameter pin to a $^5/_8$-in. thick steel plate

by G. C. Farrington

THE requirement for a high-production method of joining a 3-in. diameter steel pin to $^5/_8$-in. thick steel plate resulted in the development of a flash butt-welded joint which should find many applications.

The flash butt-welding method was considered best because it is a high-production method and once the technique and welding values have been determined the welding machine "takes over" and very little operator skill is required.

In designing a joint of this type one of the major problems is heat balance. Both plate and pin, in this case, are a low-carbon steel, such as S.A.E. 1020, so that the attaining of heat balance is a matter of physical proportions of the two parts rather than varying electrical and thermal characteristics for the two parts.

The greater mass and cross-sectional area of the pin (Fig. 1) means that it will be the more difficult part to bring to welding temperature.

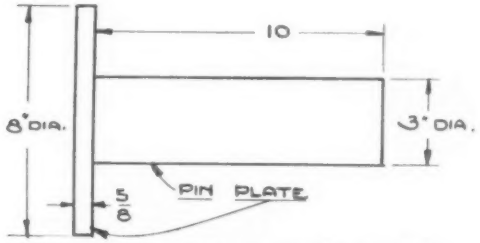


Fig. 1 Showing relative masses of the two parts

With the same amount of current flowing through the two parts the plate will reach welding temperature first. For this reason the mass and cross-sectional area of the pin was reduced in the weld region as shown in Fig. 2. The parts are shown in Fig. 3 in position for welding.

It was found by means of a few experimental welds that the design of the pin was not proper to produce sufficient heat in the pin. The strength specifications for the weld would not permit further reduction of the weld area. The parts were therefore preheated by bringing them together under force in the welding machine and permitting a low value of current to flow until the end of the pin became cherry red (approximately 1500°).

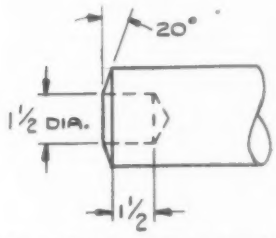


Fig. 2 Showing drilled end and chamfer required for heat balance

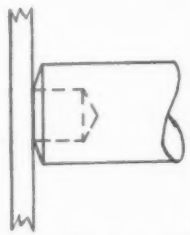


Fig. 3 Showing parts in position for welding

In the preheating operation the current was sufficiently low to prevent the occurrence of flashing. After the preheating operation the usual flash-welding operation was performed. Figure 4 shows a cross section of a welded sample and illustrates the type of weld obtained.

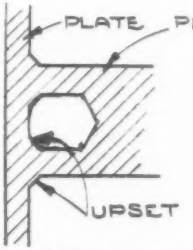


Fig. 4 Showing cross-section of welded samples

The experimental welding was done on a hydraulically operated 300-kva. flash butt welder. Cylindrically contoured dies were used for clamping the pin which was prevented from slipping in the dies by "back-ups." The plate was clamped by fingers to a flat, vertical die. Following are approximate weld values.

Open-circuit secondary potential, v. (weld)	10.8
Open-circuit secondary potential, v. (preheat)	5.0
Upset force, lb. (computed)	45,000
Average flashing current, amp.	21,000
Upset current, amp.	63,000
Flashing time, sec.	30
Upset current time, cycles	60
Flash travel, in.	$^{11}/_{16}$
Upset travel, in.	$^5/_{16}$
Die opening at start, in.	$2^1/_2$
Die opening at finish, in.	$1^1/_2$

G. C. Farrington is Welding Engineer at the Easy Washing Machine Corp., Syracuse, N. Y.

After suitable testing the weld was found to be satisfactory and a flash butt welder was designed with special clamping and control features to automatically preheat and weld the parts. Both weld and equipment have proved satisfactory in production.

Jigging for Welding Cylinder Seams

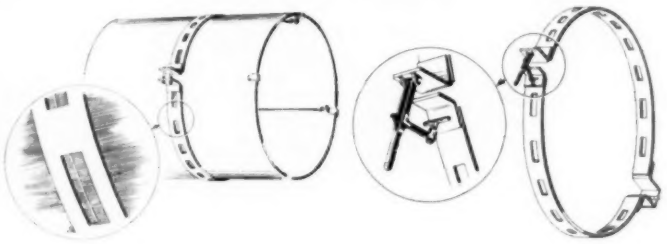
Simple jigging method solves the problem of fitting and tacking cylinder seams

IT IS not uncommon to have to join the ends of two cylinders or tubes of sizable diameter, and as the diameter increases, the problem of aligning, fitting and clamping the parts grows rapidly. The simple jigging method sketched here takes care of the problem of concentric alignment and clamping with minimum difficulty, and also permits tacking the joint quickly and easily. Since this type of girth clamp follows the contour of the work snugly, it does not interfere with rotating the work for position welding. Longitudinal clamping can be used, when required, to maintain the required fit-up of the parts.

The jig is a suitable length of strap iron, formed to a circle, provided with joints to open and close, and slotted at intervals to permit access for tacking. The sections are fastened and tightened around the parts by bolts, toggle clamps or turnbuckles at the joints. Width and thickness of the strap material, length and number of the sections, and the size and spacing of the slots will depend on the individual job.

In one case, this type of jig was used on a 10-ft. diameter tank, and the strap required, in two sections, was made of $^3/_8$- by 4-in. material, with 4- to 5-in. slots 4- to 5-in. apart. Angle clips were welded to the ends of the segments, and were drawn tight with heavy bolts. For smaller units, such as might be produced in numbers, a hinged unit with a quick-acting toggle clamp might be advisable for faster attachment and removal.

In use, the parts are brought together and clamped lengthwise as required, with the strap placed around them so that the slots open over the joint. When the strap is tightened, the two circumferences are held tightly in alignment, and after tack welding the clamp is removed to permit complete welding of the joint.



Data and illustrations furnished through the courtesy of Air Reduction Sales Co.

Repair of Hoist Drum Is Tricky

by P. A. Spaulding

HOW would you make the repair if a job like this came into your shop? One of the spokes broke on this cast-iron hoist drum and the welding shop was called to make a repair. The drum and spokes were a one-piece casting. When the hoist is being used, the spokes are under some stress. Therefore, the repair procedure must cut down the stresses to make a strong weld. Cover up the sketch and the rest of this article and figure out how you would make the repair. Then read the rest of this article.

P. A. Spaulding is Process Service Representative, The Linde Air Products Co., St. Louis, Mo.

HOW IT WAS DONE

The shop decided that the best repair could be done by oxyacetylene bronze welding. The cracked spoke was opened up to get the greatest strength and least stress. Otherwise, the two sections of the broken spoke would be drawn together slightly during welding. Then when the casting cooled, it would contract and place the weld under tension.

The edges of the break were chipped to clean metal and punch marks were made on each side of the break to check the expansion. Then the shaded areas, shown in Fig. 1, were heated. The drum flange and part of the drum body was also heated with the welding blowpipe. On the drum this area was several feet long. As these

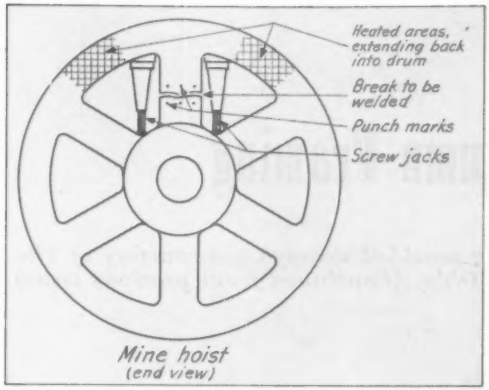


Fig. 1 This sketch shows how the broken spoke in a hoist drum was repaired by bronze welding. The shaded areas were preheated

heated areas expanded, the outer section of the broken spoke was drawn outward. This expansion movement was helped along by two screw-jacks, as shown in the sketch.

The distance between the broken ends was increased about $^{3}/_{64}$ in. This figure was decided upon by referring to the *Metals Handbook* which gave 0.042 in. as the estimated expansion of a 1-ft. length of cast iron when heated to about 500° F.

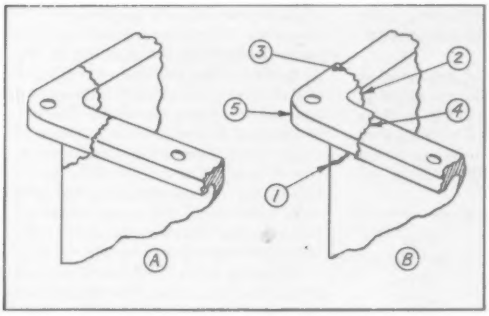
When the opening was increased to the proper distance, the break was bronze welded in the usual way. After welding, the jacks were removed and the preheated section allowed to cool and compress the weld area. As the weld cooled, it was peened lightly with a hammer.

The entire job was completed without removing the drum from its operating position. This made it possible to do a fast, efficient repair that was as strong as the original casting.

How to Repair a Cast Flange Section

Use this method to get smooth fits on your repair jobs

by H. B. Gilson



This sketch shows the welding sequence for repairing a cast-flange section by bronze welding or fusion welding

THIS sketch shows how to handle one of the commonest of repair jobs. To weld a flange on a cast-iron part, broken as at *A*, follow the sequence shown in *B*. The sequence is the same whether you fusion weld or bronze weld.

H. B. Gilson, Development Engineer, The Linde Air Products Co., Newark, N. J.

First, clean the fracture thoroughly. If the metal is thick, grind or chip to a vee. Then align the parts for welding as shown in *B*. Offset the broken section to raise it slightly above its original position. An offset of $^{1}/_{32}$ to $^{3}/_{64}$ in. is enough for fusion welding; a somewhat smaller amount will do for bronze welding.

Start at the corner of the casting body (*1*) and weld up to the flange on one side. Next, go back to the same point (*1*) and weld the other crack (*2*) through the body of the casting in the same way. Contraction in these two welds will pull the broken section into line with the undamaged portion of the flange. After welding the flange section at (*3*) and (*4*), heat the casting around section (*5*) to a dull red and let the part cool slowly to relieve stress in the repaired area.

Judgment of the exact amount of offset to be allowed in aligning the broken section with the rest of the flange comes with experience. If the amount is right, the repaired section will be flush with the rest of the flange. If too great, the section will be high and may have to be ground down slightly after welding. If too little, the section will be low and it will be necessary to build it up lightly before finishing by grinding.

Arc-Welded Beam and Column Framing

Studies in structual arc welding provided through the courtesy of The Lincoln Electric Co., Cleveland 1, Ohio. (Continued from previous issue)

FIGURE 4 illustrates how the simple beam-to-beam connections with end connection angles were made with the arc in the *Register* and *Tribune* Building.

All of the smaller sized beams in this structure were designed as simple beams with two standard connection angles at each end.

In this type of connection one angle is shop welded to the supporting girder and the other is shipped loose bolted to the girder. The outstanding legs of both of these angles are punched with a minimum number of holes so that the beam can be temporarily supported by means of drift pins or bolts through the web independently of the beam on the far side until the rest of the connection is welded. This method allows adequate erection clearances and yet insures exact span lengths for the beams and exact spacing for the main girders.

The following table shows a typical set of this type of standard welded simple beam connections such as were used on the *Register* and *Tribune* Building in Des Moines. The table gives the number of bolt holes that were provided for the erection of various sizes of beams and also the amounts of field welding for the permanent connections.

All of the smaller sized beams were designed as simple beams in this manner and the flanges of the beams were not field welded except for the main wind bracing and continuous girder connections to the columns.

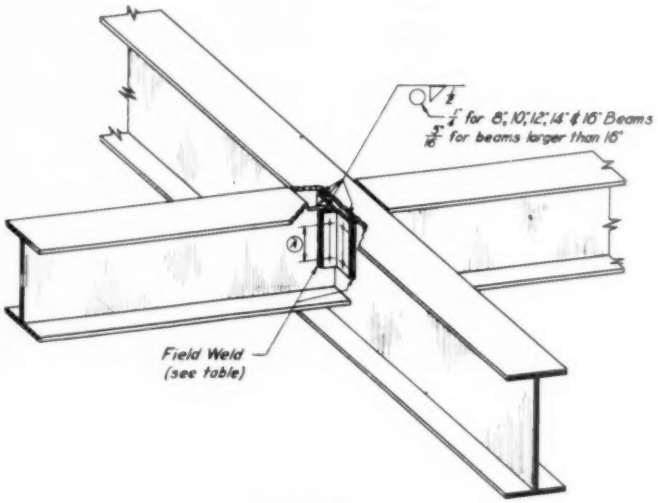


Figure 4

Beam Size, in.	*A, Spaces*	*Field weld*	*Total shear, lb.*
8 in. & 10 in.	None	1/4 in. x 0 ft. — 5 3/4 in.	13,800
12 in. & 14 in.	1	1/4 in. x 0 ft. — 8 1/2 in.	25,200
16	1	1/4 in. x 0 ft. — 11 1/4 in.	36,700
18	1	5/16 in. x 0 ft. — 11 1/4 in.	48,000
21	2	5/16 in. x 1 ft — 2 in.	65,000
24	2	5/16 in. x 1 ft. x 4 3/4 in.	83,500
27	2	5/16 in. x 1 ft. x 7 1/2 in.	101,000
30	3	5/16 in. x 1 ft. x 10 1/4 in.	127,000
33	3	5/16 in. x 2 ft. — 1 in.	144,000
36	4	5/16 in. x 2 ft. — 3 3/4 in.	161,000

In designing beam or beam-to-girder connections, the shop punching of the main members should be eliminated as much as possible, as this will materially reduce fabricating costs, since the beams or girders are large and heavy and cost considerable to handle.

At the wind brackets the continuity of the beam to column framing was gained in a unique manner as shown in Fig. 5. Bent plate seat angles, 15 1/2 x 4- x 3/8-in., were made, shop welded to the column flange with the long leg outstanding. The 3/8-in. plates were bent to a 3/4 in. radius on the inside of the bend, providing room for about 3/8-in. butt weld in line with the outstanding leg of the angle between the back of the outside radius of the bend and the face of the column flange in line with the horizontal forces as shown in Fig. 5 (*A*). At the same time, the 4-in. vertical leg of the bent plate is fillet welded to the column flange to help provide for shear. The rest of the shear is provided for by means of the 5- x 3- x 1/2-in. end connection angle. One of these angles is shop welded to the face of the column with plug and fillet welds while the other is shipped loose and field welded to the column flange and beam web after the beam is erected in the field.

Continuity in the top flange of the wind bracing beam is provided for by means of the 7- x 1 1/8-in. cover plate. This plate is shipped loose and then placed in position after the beam is erected. It is butt welded to the column first and then fillet welded to the top flange of the beam.

This connection provides for the bending moment capacity of the beam combined with an end shear of 65,000 lb. Suitable backing plates are also provided between the column flanges as necessary to resist the tension and compression forces without bending the column flanges.

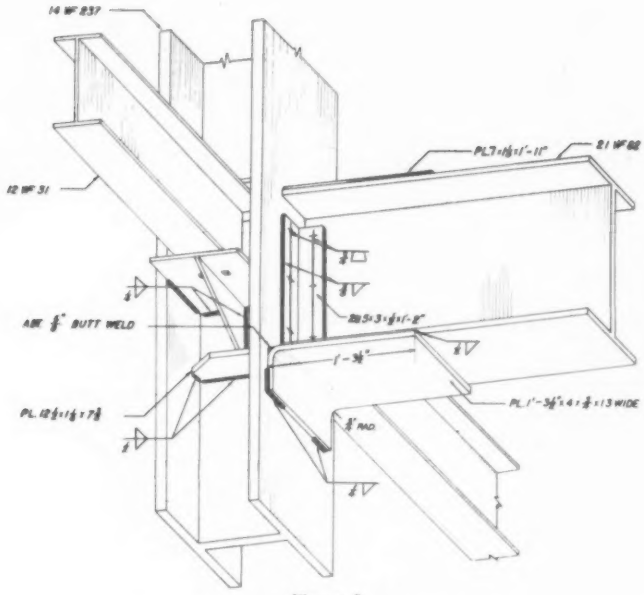


Figure 5

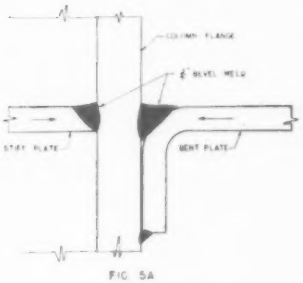


Figure 5 (A)

Figure 6 is a sketch of a typical column splice that was used on the new 10-story addition to the *Register* and *Tribune* Building, in Des Moines, Iowa, for the *Tribune*'s new radio station and newspaper plant. The splice shown is the junction of a 14 WF 176-lb. column on top of a 14 WF 228-lb. column which occurs 2 ft. above the third-floor level.

The ends of both of these column sections are first milled for a square bearing surface and then the two lower inside 4- x 3- x ⅜-erection splice angles are shop welded on opposite sides of the web of the heavier column section so as to project past the end of the column. The outstanding legs of these angles are provided with holes for erection bolts to the outstanding legs of the other two angles that are shop welded to the upper column section. The purpose of these angles being to splice and hold the two adjacent column sections together temporarily while they are being field welded.

The flanges on the lower end of the upper column section are V-beveled or J-grooved for welding and the field butt splice is completed by filling these spaces with weld metal. The bevels on both flanges are made from the same side to save handling and turning the column during the process in the shop and for better convenience of welding them in the field.

Figure 7 is a similar splice wherein a 12 WF 53-lb. column is joined just above the level of the sixth floor to the top of a 14-in. WF 87-lb. column with the aid of two flange splice plates.

Two 4⅜ x ⅞ x 1 ft. 3-in. splice plates were fillet welded to the web and inside face of the column flange on one side of the 14-in. column and then milled with the lower column section, while one 14- x 1- x 1-ft. 3-in. cover plate, with the lower end prepared for welding, was fillet welded to the outside face of the opposite flange of the 12-in. column.

In this fashion both flange welds are made accessible from the same side of the column for welding and for the convenience of erection as is sometimes necessary when new steel is erected adjacent to an old structure. The other details, splice angles, field bolts, etc., for this splice are similar to those previously shown in Fig 6.

The column splice details used for splicing 12-in. columns to 12-in. and 10-in. columns to 10-in. were also similar to that of Fig. 6. The following table gives the data for these other typical splices.

	Weld A		
Size of upper Column	*Depth of chamfer, in.*	*Length of weld, in.*	*Capacity, 1 weld, lb.*
14 WF 142 to 14 WF 426	½	15¼	113,000
14 WF 87 to 14 WF 136	⅜	14½	80,000
Columns with flanges 12 in. wide	⅜	12	65,000
Columns with flanges 10 in. wide	5/16	10	44,000
Columns with flanges 8 in. wide	¼	8	27,000

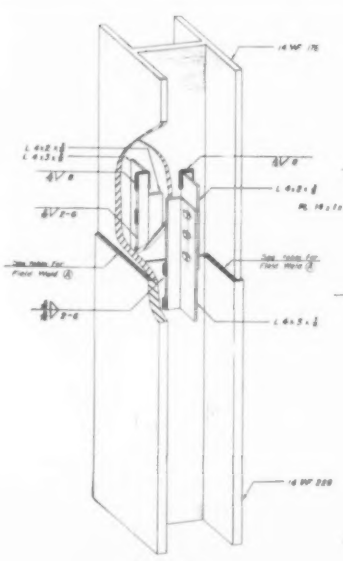


Figure 6

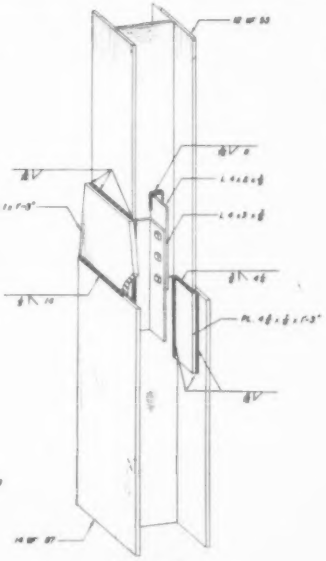


Figure 7

the AMERICAN WELDING SOCIETY

activities • related events

OUR NEW PRESIDENT

O. B. J. Fraser

O. B. J. Fraser is Assistant Manager of the Development and Research Division of The International Nickel Co., Inc., New York. Born in Brookville, Ontario, he received a B.Sc. degree in metallurgical engineering in 1916 from Queen's University, Kingston.

Mr. Fraser joined The International Nickel Co. in July 1917 as metallurgical engineer at the Bayonne, N. J., Works. In April 1918 he was transferred to the company's Port Colborne, Ontario, Works and became successively superintendent of the Electrostatic Fume Precipitation Plant, night works superintendent and research engineer. During 1922, while still with the company, he became associated with the Mellon Institute in the study of corrosion of nickel and nickel alloys. Mr Fraser was placed in charge of the Research Laboratories of INCO at Bayonne in 1924, remaining in that position until 1932. During the next two years he carried on field development work in the use of nickel alloys in the petroleum industry.

Mr. Fraser is actively identified as both officer and member of committees of a number of metallurgical and engineering organizations. He is First Vice-Chairman of the A.W.S.-A.S.T.M. Committee on Filler Metal.

Mr. Fraser joined the AMERICAN WELDING SOCIETY in 1935 and served as Treasurer of the Society (1941–47) as well as Second and First Vice-President, respectively. He is currently Chairman of the National Membership Committee and has been doing quite a spell of visiting sections for the last few years. Mr. Fraser is also a member of a number of other societies.

1ST VICE-PRESIDENT

Harry W. Pierce

Harry W. Pierce graduated from the United States Naval Academy in 1922, postgraduate in Naval Construction from Massachusetts Institute of Technology 1925–26. He served as Naval Constructor, U. S. Navy until 1930. Has been connected with the New York Shipbuilding Corp. since 1930. Most of his work during eighteen years has been closely connected with welding in ship construction. At present he is Assistant to the President.

Mr. Pierce is a native of Nebraska, and started out studying chemistry at the University of Nebraska before World War I diverted him into the Navy. During the war years, in addition to his duties at the New York Shipbuilding Corp., Mr. Pierce handled several projects for the Bureau of Ships, Navy Dept., and received the Certificate of Commendation of the Bureau of Ships. Also one of a Special Committee for the investigation of the Tanker *Schenectady* in January 1942, and has since served on many Advisory Committees in connection with the investigation of welding stresses in ships, steel for ship construction and corallary investigations.

In 1947 Mr. Pierce was granted a four months' leave of absence from New York Ship to join Overseas Consultants, Inc., for a study of the Japanese Shipbuilding industry for the War Dept., in connection with reparations and determination of minimum industrial levels in shipbuilding.

He is Vice-President of District No. 2 of the AMERICAN WELDING SOCIETY, having previously served as Chairman of the Philadelphia Section, Chairman of the Marine Committee, Chairman of the Technical Activities Committee and as a Director-at-Large.

Mr. Pierce is also a member of the Society of Naval Architects and Marine Engineers, the American Society for Testing Materials, Director of the Engineers Club of Philadelphia, and honorary member of the Japan Welding Society.

He is Vice-President of the Engineers' Club of Philadelphia, and next year's President of the Philadelphia Maritime Society.

2ND VICE-PRESIDENT

Charles H. Jennings

Charles H. Jennings was born in Des Mones, Iowa, Jan. 23, 1906, and attended Iowa State College at Ames, where he received the Degree of Bachelor of Science in Mechanical Engineering in 1928.

Mr. Jennings was admitted to the Graduate Student Course of the Westinghouse Electric Corp. in 1928, and completed the normal year's course in seven months.

There's More than One Way to Bear Down on Costs

By James B. Scott, President
Welded Products Company, Grand Haven, Michigan

When the backfield runner hits the line and finds a stone wall, he tries to skirt around it for yardage. Likewise, many shops follow these tactics in the tussle for lower costs.

When we can make no further headway in cost reduction with an existing method, we often manage to find new, fresh approaches to the problem. For example:

1. Examine the design. To improve the cast sprocket for a chicken feeder, we used two stampings as shown in Fig. 1. The resultant sprocket operates better because the feed doesn't jam in the teeth. To get the same results with the former design would have cost considerably more.

2. Examine the set-up. We were nip and tuck with competition (another process) on a machine base. By devising an improved clamping and positioning fixture, we speeded up output from 5 bases to 9 bases per day —cut unit cost $2.20 each.

3. Examine the welding procedure. Types and sizes of electrodes . . . amperage . . . A.C. or D.C. . . . these factors are weighed in setting up the procedure for each job. On a recent cold-rolled steel job, we used "Shield-Arc LH-70" electrodes and eliminated pin-holes, speeding up welding and cutting costs.

In all of our efforts to bear down on costs, we find it extremely helpful to have the advice of the Lincoln Welding Engineer, and we use Lincoln Welders and Electrodes practically 100%.

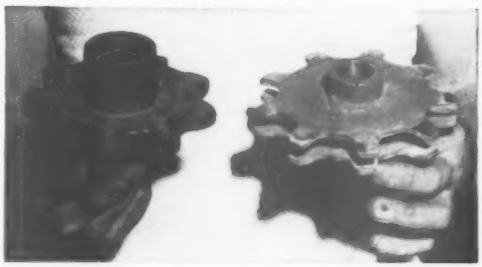


Fig. 1. Changeover of this chicken-feeder sprocket to welded stampings eliminated feed jamming.

Fig. 3. Typical of the welding output of Welded Products Company is this group of engine brackets, fabricated from stampings and shear-cut plate.

Fig. 2. Two points of attack on welding costs: Left—Duane Brady and James Scott work on the modification of a product design. Right—Ingenuity is applied to the design of fixtures and the planning of welding procedure for each product.

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In 1929 he became attached to the mechanics Division of the Westinghouse Research Laboratories in East Pittsburgh, where he instituted advanced studies in the mechanical properties of welds, welding design, welding applications and welding problems.

In 1936 he was transferred to the Laboratories' Chemical and Metallurgical Dept. and put in charge of all the welding research. In 1943 he was made Section Engineer of the Welding Section and in 1945 promoted to Engineering Manager of the Welding Department which position he now holds.

In 1937, Mr. Jennings was called to England for three months as consultant welding engineer for the English Electric Co. He has also conducted a course in welding metallurgy at the Carnegie Institute of Technology, prepared a training course for welding operators for the Pittsburgh Board of Education and served as part-time staff member of the N.D.R.C. War Metallurgy Division during 1942. He is the author of more than thirty articles on welding and two books—*How to Weld 29 Metals* and *50 Lessons in Arc Welding*.

In 1939, Mr. Jennings was awarded the Westinghouse Silver "W" and Order of Merit for distinguished service in welding research and application. Mr. Jennings has been active in the AMERICAN WELDING SOCIETY for the past twenty years and has held the offices of Chairman Pittsburgh Section, District Vice-President, Director at large and Director Niagara Frontier Section. He is Chairman of the Technical Activities Committee and a member of seven other committees. He has served on six other committees, assisted in the preparation of several chapters for the 1938 and 1942 editions of the *Welding Handbook* and lectured before thirty-eight sections of the SOCIETY.

VICE-PRESIDENT, DISTRICT NO. 1

H. S. Swan

Our new District Vice-President, H. S. Swan, was born on Feb. 11, 1908; graduated from Rensselaer Polytechnic Institute in 1940 with the Degree of Civil Engineer. From 1931 to 1940 he served as Research Welding Engineer for J. A. Roebling's Sons Co. From 1940 to 1941 he was Assistant Welding Engineer in the New York Navy Yard. In that year he became connected with the American Locomotive Co. as a Welding Engineer and served in that capacity until 1944 when he was made General Welding Supervisor. In 1947 he received a further promotion and became Welding Engineer for the Locomotive Division which position he occupies today. Mr. Swan is Past-Chairman of the Northern New York Section of the AMERICAN WELDING SOCIETY, Past-Chairman of the Schenectady Engineering Council and is a member of the Army Ordnance Armor Welding Committee.

VICE-PRESIDENT, DISTRICT NO. 3

L. C. Stiles

L. C. Stiles, Plant Superintendent, Chicago Bridge and Iron Co., Birmingham, Ala., has been elected Third District Vice-President of the AMERICAN WELDING SOCIETY.

He was born Feb. 20, 1906, in Strafford, N. H., attended the public schools at Greenfield, Mass., and graduated from Ohio Northern University in 1928 with a B.S. in Civil Engineering. Upon graduation he went to work for the Chicago Bridge and Iron Co., in their Erection Dept. where he immediately became involved with field welding problems. He

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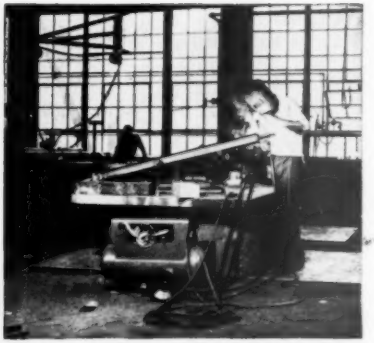
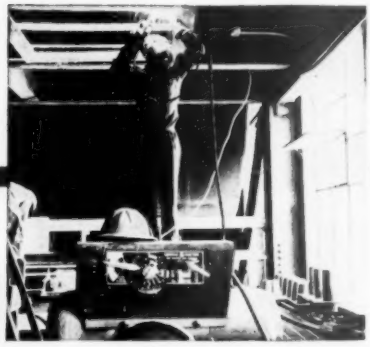


This G-E WD-42 200-amp welder is in constant use on a great variety of jobs in this welding job shop. Like the 300- and 400-amp models, it gives superior welding performance with a forceful, easily controlled arc over a wide current range.

2



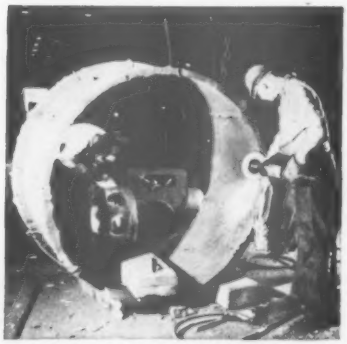
Here's one of 14 G-E d-c welders employed on a big power-plant construction job. Note the single-dial, dual control—it's standard on all three models. The handy electrode selector makes desired current settings easy, and gives complete flexibility.

3

The 200-amp WD-42 welder is ideal for welding with low current on sheet-metal and other thin gauge materials. All WD-40 welders offer a 50% savings in size and weight over heavy, old-fashioned welders—thanks to efficient functional design, which has eliminated "dead-weight."

4

This WD-44 400-amp welder is a primary maintenance tool in the main generating station of a Massachusetts power company. And maintenance is simple and inexpensive on all G-E welders.



5

Plenty of heavy usage is the regular diet of this WD-43 300-amp welder in a New England foundry, and it's always ready for more; dependable service over a long "life-span" is one of its big features.

for 5 welding jobs

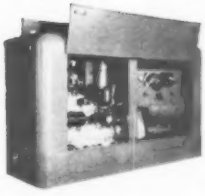
(and many more)

200-amp welder with Wisconsin air-cooled engine and V-belt drive; welding generator and control is identical to WD-42 motor-generator set. Also available with Hercules *liquid-cooled* engine. Note the auxiliary power take-off (d-c) for 110-v lights and universal tools. Approximate weight—1000 lbs.

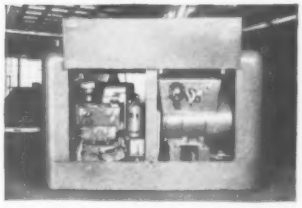
. . . engine-driven welders? Certainly!

In addition to the same advantages and welding characteristics of the motor-generator welders, G-E engine-driven welders also provide:

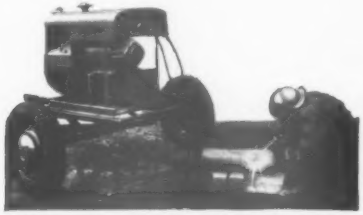
1. Easy, dependable operation with performance-proved engines.
2. Light weight, and a variety of trailer models, for ready portability.
3. G.E.'s "slow-down" control saves gas, and wear and tear by slowing the engine to idling speed when not actually welding; engine speeds up automatically the instant the arc is struck, with a precision governor to prevent overspeeding.
4. Engines operate at *constant* optimum speed; all welding control is electrical.
5. Muffled exhaust with rain cap to exclude water.



300- and 400-amp models are available with either 6-cylinder Chrysler gas-engine drive, or with. . .



. . . GM or Caterpillar diesel-engine drive. Electric starting is furnished on all 300- and 400-amp models, both gas and diesel, at no extra cost.



For light-duty welding this WD-3200BG 200-amp machine with air-cooled engine-drive is ideal; weighs only 660 pounds (net) and is only 26" wide.

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Fuse plug and bursting disc provide positive safeguard.

Bronze stem furnished as standard, also available with monel stem on special order.

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Ball-nose seat construction assures positive shut-off.

for **CARBON DIOXIDE**

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Compact, rugged design for use with high pressures.

Equipped with bursting disc safety device.

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L. C. Stiles

has supervised the field welding of practically every type of structure built by his company, including the construction of 157 LST's at Seneca, Ill. during World War II.

In 1945 he was sent to his company's Birmingham plant where he served as Welding Engineer until 1947 when he was made Plant Superintendent.

"Dan" Stiles, as he is more commonly known, has been a member of the AMERICAN WELDING SOCIETY for many years and was Chairman of the Birmingham Section in 1948–49. He is also a member of the American Society for Metals.

VICE-PRESIDENT, DISTRICT NO. 5

John A. Grodrian

John A. Grodrian was born in Fort Wayne, Ind., May 24, 1910, attended Antioch College 1927–28 and graduated from Purdue University in 1934 with a Bachelor Degree in Chemical Engineering, majoring in Metallurgy.

Mr. Grodrian has had a wide industrial experience working for the General Electric Co., from 1934 to 1936; in the Metallurgical Laboratory of Carnegie-Illinois Steel, 1936 to 1939 and as Technical Trade Consultant of the latter company in their Alloy Bureau from 1939 to 1942. He became connected with the Bendix Products Division of Bendix Aviation Corp., in 1942 and is now connected with that company. He started as staff Metallurgist, was promoted to Chief Metallurgist and in 1946 was promoted to Director of Factory Laboratory, which position he now holds.

Principal interest in welding embraces technical control of welding of high-alloy steels employed in aircraft landing gear construction, and brazing of special materials. He assisted in developing satisfactory commercial method of copper brazing stainless steels.

Mr. Grodrian is a Founder member of the Michiana Section and has served as Vice-Chairman and Chairman of that Section and has lectured to numerous Sections on Welding Metallurgy.

He is a member of the Executive Committee of the Notre Dame Section of the A.S.M. and has served as Vice-Chairman and Chairman of that Section. Mr. Grodrian is also a member of the National Association of Foremen, and Izaak Walton League of America.

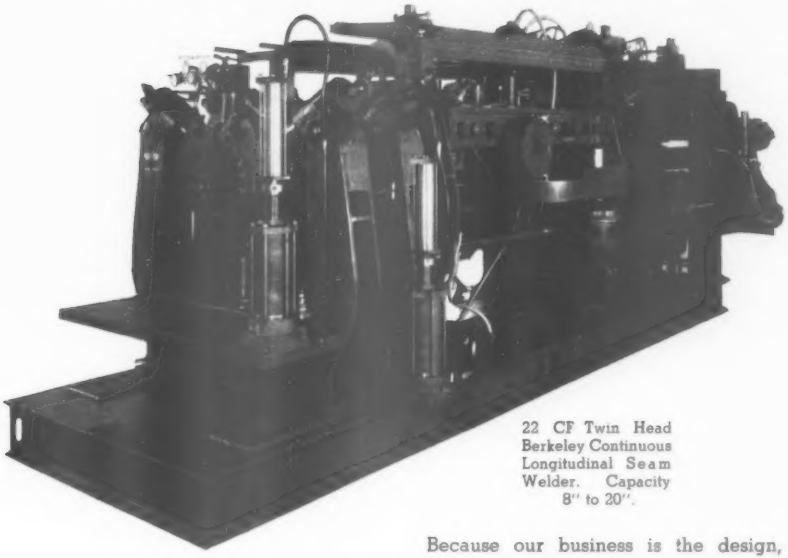
VICE-PRESIDENT, DISTRICT NO. 7

W. F. Boyle

Mr. Boyle was born in Brooklyn, N. Y., in 1905 and graduated from Pratt Institute, Brooklyn, in 1927 as an Industrial Mechanical Engineer. He completed a Graduate Student Course with the Westinghouse Electric Corp., and served in the Large Turbine Section of that company at Essington, Pa., through 1936.

Thereafter, he was transferred to the Westinghouse Middle Atlantic District with headquarters at Philadelphia, Pa., and served for nearly three years as a

STAYING OUT OF THE GROOVE IS OUR BUSINESS!



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- Turntables
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- Turning Rolls
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No two Penn Tool & Machine Co., products are alike because each of our customers has a unique requirement—a special problem to be met and solved by a welding jig or fixture.

But no matter how different one Penn Tool & Machine Co. product is from another, they all have the same sound design, practical engineering and superior operating efficiency that has characterized Penn products for many years.

If your production requires the welding of repetitive parts and your costs must be kept to a minimum, bring your problem to us. Our engineering skill, experience and modern plant facilities are at your disposal.

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DANVILLE ILLINOIS

Steam Engineer, responsible for Application and Installation Engineering on steam turbines and condensers, reduction gears, stokers and related steam power and electrical equipment. Following this he was returned to Westinghouse Steam Division, Essington, Pa., and organized and served as Manager of the General Purpose Turbine Sales Section, holding this position until being made Manager of Marine Sales in 1941.

A year later Mr. Boyle was shifted to the Manufacturing Dept. to set up a Subcontracting Division and served as its Manager until 1944, at which time was made Manager, Gas Turbine Activities, with the responsibility for coordinating the efforts of all departments on the critical and revolutionary Navy jet airplane power plant development.

When Westinghouse organized an Aviation Turbine Division on Feb. 1, 1945, he was appointed Sales Manager. He resigned from this position in May 1945 to join the Baldwin Group as Assistant to the Vice-President, The Baldwin Locomotive Works, Eddystone, Pa. On Aug. 1, 1945, Mr. Boyle was appointed by the Board of Directors to serve in a dual capacity as General Manager of The Pelton Water Wheel Co., and Pacific Coast District Manager, The Baldwin Locomotive Works, San Francisco, Calif.

On Nov. 27, 1946, at a meeting of the Board of Directors, he was elected Vice-President and General Manager of The Pelton Water Wheel Co., and has since served in this capacity.

Mr. Boyle is registered in the state of California as a Professional Engineer, Mechanical Branch, July 1, 1948.

Mr. Boyle is Director of the California Metal Trades Association and has served as Chairman of committees of that association, of which he is also a member of the Executive Committee. He is also Vice-President of the Pacific Coast Committee of the Newcomen Society of England. He is a member of the M.A.P.I. Council for Technological Advancement, the Olympic Club of San Francisco and the AMERICAN WELDING SOCIETY. He is also an active member of the California State Chamber of Commerce and the California Manufacturers Association.

NEWLY ELECTED MEMBERS, BOARD OF DIRECTORS

Leon C. Bibber

Leon C. Bibber graduated from the University of Michigan in 1916 with the degree of Bachelor of Marine Engineering. During the following year he received his M.S. at the same school. From 1918 to 1926 he was engaged in designing and estimating work on marine machinery and other types of structures.

In 1926, Mr. Bibber began his work on the design and construction of welded buildings, cranes, ships, etc. In 1929 he became Senior Welding Engineer of the Bureau of Construction and Repair of the United States Navy Dept., Washington, D. C. There he was involved in laying the foundations for the use of welding in naval ship construction.

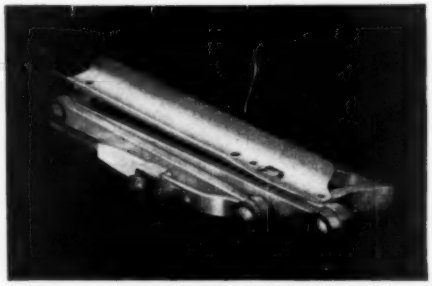
In 1936 he became Welding Engineer of the Carnegie-Illinois Steel Corp. in Pittsburgh. Here he is concerned with research into the weldability of the various products manufactured by that company, and in the applications of those products to welded construction.

He is a member of many technical societies and many committees of those societies.

A. F. Davis

Alton Frank Davis was born in Diamond, Ohio, Oct. 24, 1889. He graduated from Ohio State University in 1914 with the degree of Electrical Engineer.

Upon his graduation from Ohio State University, he started to work for The Lincoln Electric Co., Cleveland, Ohio, serving the company in several positions. In 1925 Mr. Davis became a director and was elected a vice-president of the Company and then in addition secretary in 1938.



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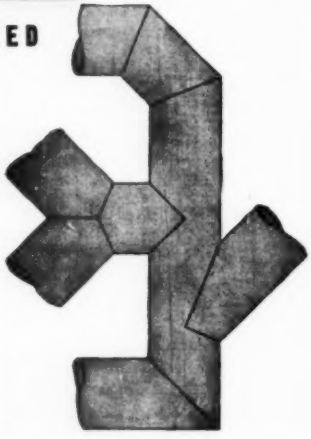
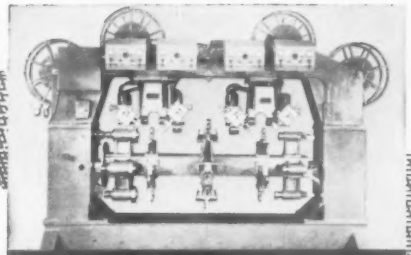
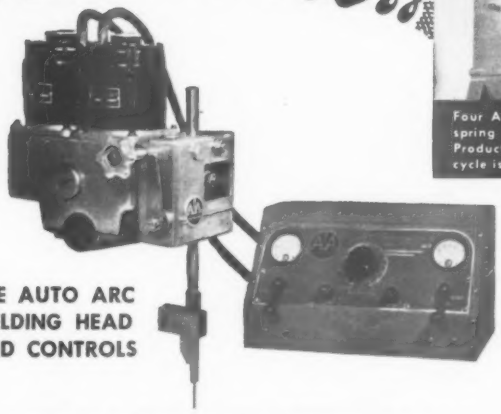


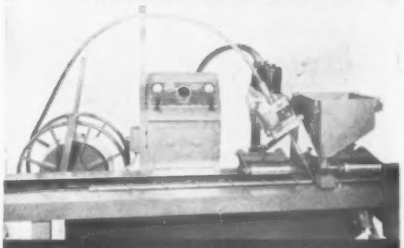
Versatile...



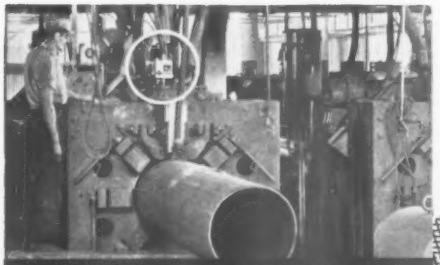
Four Auto Arc Welding Heads automatically weld spring pads on passenger car rear axle housing. Production rate is four housings per minute; welding cycle is 11 seconds per housing.



THE AUTO ARC WELDING HEAD AND CONTROLS



An Auto Arc Welding Machine for fabricating augers. The auger moves under the arc for welding the flight to the tube. The machine may be set for continuous welding.



Auto Arc Welding Heads are utilized in the continuous production of large diameter, high pressure pipe. Both A. C. and D. C. welding are used, with each arc supplied from a 1500-ampere capacity unit.

is the word for AUTO ARC

The versatility of the standard Auto Arc Welding Head is illustrated by the range of applications. The production of 16 open-arc welds per minute with four heads requires good arc starting features. Another installation with two heads starting simultaneously with 380 amps. A. C., submerged-arc on 5/32" electrode, has a welding cycle of 30 seconds.

Continuous welding is maintained in a pipe mill where uniform arc control is necessary to secure desired penetration. After welding, the pipe is expanded hydraulically with pressures of 1500-2000 lbs. p.s.i.

Auto Arc equipment is also successfully operating on steel mill applications of hard surfacing materials in high temperature zones.

Where automatic arc welding is applicable to your production, let our engineers estimate its economy.

THE AUTO ARC-WELD MFG. CO.

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For the past 10 years almost everything for cutting and welding has been exclusively NCG at the Hillman Barge and Construction Co.'s Alicia yard near Brownsville, Pa. Hillman, designers and builders of barges, towboats, derrick boats and other craft, produce the all-welded streamlined, twin-screw, diesel-motor tows of the type seen in the picture with Sureweld B and N electrodes. NCG gases and apparatus, both manual and automatic, are used to speed shop fabrication of structural parts.

At the Richmond, Ind., plant of Wayne Works, world's largest manufacturers of school buses, Sureweld electrodes are used. Safe construction is paramount, and the closely inspected welds must be strong, and clean, too, to permit fine finish. Here are seen an arc-welding team joining seat legs to frames in the bustling Wayne factory. Wayne Works also are users of NCG gases.

To be sure it's Sureweld

Production is speeded at the Indianapolis plant of J. D. Adams Manufacturing Co., oldest manufacturers of road machinery and pioneers in all-welded construction, by the use of NCG's Shield-O-Matic automatic welding process. Here is seen the welding of discs to rims in the fabrication of wheels for Adams graders. Adams also uses three NCG automatic flame cutting machines to keep their heavy production moving fast, and relies on NCG gases

Proved performance makes SUREWELD electrodes the choice of thousands of steady users...

"Sure, Sureweld Electrodes do the job better—*and* faster, easier and more economically," agree thousands of satisfied Sureweld users from coast to coast in *every* line of manufacturing you can name. The explanation is simple . . . Sureweld Electrodes are manufactured under the most exacting conditions, safeguarded in quality by *four* distinct laboratories, rushed to you *fresh* through NCG's nation-wide network of branches and distributors. And built into every Sureweld Electrode is NCG's *30 years of experience* in welding application . . . *Sure*, whatever the job, there's a Sureweld rod to bring new speed, quality and economy into *your* welding operation. Try Sureweld today *for sure!*

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The board of directors of The Lincoln Electric Co. created The James F. Lincoln Arc Welding Foundation in 1936 in honor of J. F. Lincoln, president of the company. Mr. Davis was appointed secretary of the Foundation. Its purpose is to stimulate scientific progress in arc welding.

In 1942, Mr. Davis established at the Ohio State University the A. F. Davis Welding Library and presented it with funds for its continued growth.

In 1945, Mr. Davis established the A. F. Davis Welding Awards with the AMERICAN WELDING SOCIETY, wherein awards are given yearly for papers on welding written by undergraduates of engineering colleges and published in undergraduate magazines.

Mr. Davis is Vice-President of the Board of Trustees of Glenville Hospital and a member of the executive committee of the Board of Trustees of the Cleveland Hospital Service Association. He is on the Board of Trustees of the Cleveland Young Men's Christian Association and holds memberships in the AMERICAN WELDING SOCIETY, American Society for Engineering Education, American Society for Metals, American Society of Mechanical Engineers, American Society for Agricultural Engineering and American College Publicity Association. His college fraternity is Sigma Alpha Epsilon. He also is a member of Eta Kappa Nu, honorary electrical fraternity. Other club memberships include the Rowfant Club, the Canterbury Country Club, the Mayfield Country Club, the Union Club, the City Club, the Cleveland Chamber of Commerce and the Mid-Day Club, all of Cleveland, the Faculty Club of Columbus, Ohio, and the National Press Club of Washington, D. C. He is a member of Fairmont Presbyterian Church.

A. J. Moses

A. J. Moses, a graduate of Georgia School of Technology, has been connected with Combustion Engineering-Superheater, Inc., or its constituent companies, since 1920. He became General Manager of the Chattanooga Division in 1937, and in 1942 was elected Vice-President.

An authority on welding and X-raying, Mr. Moses is the author of numerous articles and papers on welding and the fabrication of welded pressure vessels.

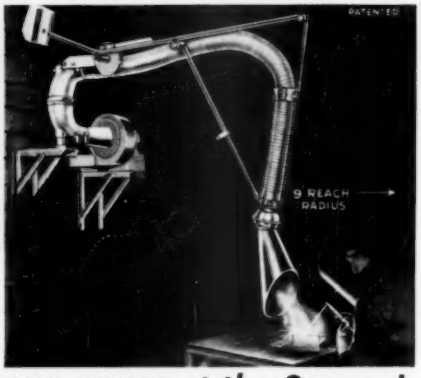
For many years, Mr. Moses has been a member of the AMERICAN WELDING SOCIETY, the American Society of Mechanical Engineers and the American Society for Testing Materials, having served on various committees of these societies. He is also active in civic and educational organizations.

Gilbert S. Schaller

Gilbert S. Schaller graduated from the University of Illinois with a B.S. in M.E. degree in 1916. Upon graduation, he was employed by the Kawneer Mfg. Co., Niles, Mich., in the production engineering department. While employed there, he developed a machine for welding tubing with a multiple tip torch—a development that played an important part in aircraft tubing manufacture during World War I. After attendance in Officers' Training Camp at Fort Sheridan, Ill., he was commissioned in the Ordnance Dept. He served overseas for eighteen months both with the A.E.F. and as a staff officer in the Fourth French Army. Upon resumption of civil status he was with the Mendota Manufacturing & Transfer Co. in production work and later joined the H. D. Conkey Co., as Factory Manager. From the latter position, he went to the University of Washington, Seattle, as an Instructor in Mechanical Engineering. In 1925, he earned the M.B.A. degree at that university. He is now Professor of Mechanical Engineering in charge of the manufacturing methods courses and laboratories.

Professor Schaller is a member of Tau Beta Pi, Sigma Xi and Chi Phi. Twice he has been chairman of the Puget Sound Section of the A.W.S. and a contributor to THE WELDING JOURNAL as well as other technical publications. He is a member and Past-Chairman of the Puget Sound

RUEMELIN Fume Collector



(Above) Welding without ventilating snout. Note smoke clouds and poor vision. Gases and heat surround operator's helmet.

(Right) Collecting fumes with a Ruemelin Fume Collector.

REMOVES WELDING FUMES *at the Source!*

Solve your welding fume problem efficiently by installing Ruemelin Welding Fume Collectors. They produce a powerful suction that removes smoke, gas and heat at the source. Guards employee health, resulting in less welder fatigue. Clears shop air with minimum heat loss. Covers maximum welding area vertically, horizontally and by circle swing. Made in 9 ft. and 15 ft. reach sizes. Shipped assembled, easy to install. Thousands in practical service. Free engineering service for your installation.

Write for our New Bulletin 37-D and list of users — just off the press.

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Chapter of the A.S.M., and a member of the A.F.A. where he has served on several national committees. Among other publications, Prof. Schaller has won the Junior Prize of the A.S.M.E. During the late war, he served as consultant for several Seattle and Tacoma companies in both welding and foundry capacities.

American Welding Society Announces 1949 A. F. Davis Undergraduate Welding Award Winners

In his first attempt at technical writing James B. Snider, Jr., Agricultural and Mechanical College of Texas (Texas A. & M.), College Station, Tex., won the coveted first prize in the 1949 A. F. Davis Undergraduate Welding Award. Besides a $200 cash award Mr. Snider will be presented with a certificate in honor of his achievement on Monday, Oct. 17, 1949, during the annual meeting of the AMERICAN WELDING SOCIETY at Cleveland, Ohio.

David S. Wise, University of Michigan, Ann Arbor, Mich., has been named the winner of the second prize of $150 and will also be honored at the A.W.S. annual meeting.

This Award is sponsored by the AMERICAN WELDING SOCIETY and is made possible by funds donated by A. F. Davis, Vice-President, The Lincoln Electric Co., Cleveland, Ohio. The Award consists of

D. S. Wise

four cash prizes given annually to authors and publications for the best and second best articles on welding published in undergraduate papers or magazines during the preceding year.

The *Texas A. & M. Engineer*, which published Mr. Snider's article entitled "The Atomic and Crystalline Structure of Metals" in its December 1948 issue, will also receive a $200 cash award. The *Michigan Technic* will be presented with a $150 prize for publishing the article by Mr. Wise entitled "Electrical Welding in Conjunction with the Briquetting Process" which appeared in its November 1948 issue.

Mr. Snider, who received his B.S. in Mechanical Engineering in August 1949, is a native of Waco, Tex. He was a member of the R.O.T.C., Ross Volunteers, American Society of Mechanical Engineers and the Waco A. & M. Club at Texas A. & M.

Mr. Wise is a native of Cleveland, Ohio, and a senior in Electrical Engineering at the University of Michigan. He was elected earlier this year to Phi Kappa Phi, National Honorary Scholastic Fraternity.

The Importance of Welding to Management, the Engineer and the Public

A unique and outstanding session is planned for the coming Annual Meeting of the American Welding Society on Thursday evening, October 20th, 8 p. m., Allerton Hotel, Cleveland, Ohio.

Phil Swain, Chief Editor, Power and Operating Engineer, McGraw-Hill Publishing Co. will present a comprehensive non-technical dissertation on the subject. He will cover developments in the automotive, heavy appliance, structural, home appliances, transportation and other industries.

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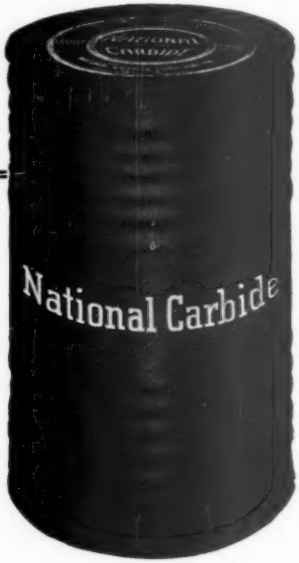

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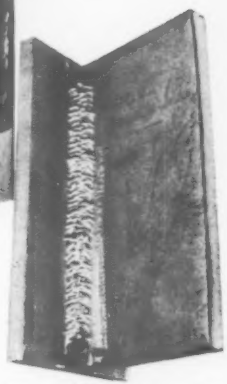

Specimen of AIRCOMATIC welding stainless -- vertical down.

now it can be done!

weld stainless steel with new high-speed AIRCOMATIC process

Now, stainless steels in all standard joint designs, in thicknesses of ⅛ inch plus, can be welded faster *in all positions.*

You can make single or multi-layer welds by either beading or weaving. Vertical welds can be made upward or downward. In short, the Aircomatic Process permits the welding of stainless with all the ease and versatility of ordinary steels – at a high rate of deposition and with complete freedom from slag.

Continuous feeding of filler metal, and inert gas (helium or argon) shielding, are the two main features that give this new welding method its exceptional speed and flexibility. A bare filler metal, in wire form, is fed continuously through a manually-operated gun . . . and, of utmost importance, the process deposits a weld metal with an analysis almost identical to the filler wire – even approximately 60% of the titanium in the filler wire is transferred to the deposited metal.

Investigate the many advantages this new *Aircomatic Welding Process*, for welding stainless steels, aluminum and other metals, can offer you. If you can't visit our booth at the Metal Show, but desire more information in the form of a descriptive folder, delivery and price data, fill in and mail the coupon below.

SEE THE AIRCOMATIC WELDING OF STAINLESS STEEL AT THE AIRCO BOOTH No. 2126, NATIONAL METAL SHOW, CLEVELAND, OCT. 17TH TO 21ST, 1949

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Please send me your folder (ADC-661) describing the welding of stainless steel with the Aircomatic Process and equipment.

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NEWS OF THE INDUSTRY

A.S.T.E. Semiannual Meeting

Details of the program for its 17th semiannual meeting announced by the American Society of Tool Engineers reveal that it will be featured by one of the most extensive technical programs in the Society's history.

Scheduled for the Mount Royal Hotel in Montreal, October 27th through 29th, the three-day program provides for nine technical sessions—three on Thursday, five on Friday and one on Saturday.

The majority of the sessions deal primarily with methods by which tool engineers can reduce costs. Cost cutting with tools, cutting costs on dies, cost cutting with machines, estimating and economics of cost cutting are among the major subjects to be tackled at individual sessions.

Opening the program officially on Thursday, October 27th, will be a welcoming luncheon at which Camillien Houde, Mayor of Montreal, will welcome members to the city. Technical sessions begin at 2:00 P.M. with papers on "Mold Die Hobbing," "Mold Die Finishing" and "Application of Standard Tool Parts to Cut Costs."

Thursday evening is devoted to a session on "A New Concept of Surface Measurement," with two papers.

"Use of Low Melting Point Alloy for Tool and Die Work" will be the subject of a paper Friday morning and another on "Special Purpose Machines from Standard Units" at another session. "Estimating and Economics of Low Cost Tooling" is the topic of one of Friday afternoon's sessions.

A symposium on all the factors relating to lowering costs in "Limited Production," is also scheduled for Friday afternoon. Friday evening's session will be devoted to various phases of "Hot Machining."

Saturday morning's session is devoted to the subject of "Statistical Quality Control."

Plant Visits

Numerous plant visits have been scheduled in the neighborhood of Montreal to permit visiting tool engineers to inspect methods by which Canadian industry has raised itself from eighth to fourth place in industrial output in the world. Plants at which visits have been arranged include Canadian Car and Foundry Co., Ltd.; Canadian Vickers, Ltd.; Dominion Engineering Works, Ltd.; Telephone Division and Wire and Cable Division of Northern Electric Co., Ltd.; RCA Victor, Ltd.; Canadair, Ltd.; Canadian Pacific Railway Co., Angus Shops; and Montreal Locomotive Works, Ltd.

Clarence D. Howe, Banquet Speaker

Canada's Minister of Trade and Commerce, and also of Supply and Reconstruction, the Honorable Clarence D. Howe, will be the guest speaker at the semiannual dinner Saturday evening, speaking on "Industrial Canada—Today and Tomorrow."

Ladies Program

An extensive ladies' program has also been developed for the three-day meeting including special scenic motion pictures, a motor tour through the Laurentian Mountains, visits to interesting local industries, a fashion show, etc.

Lehigh Research Engineer to Go to England

Lynn S. Beedle, research engineer for the department of civil engineering at Lehigh University, has been granted a leave of absence to accept an invitation from Cambridge University, England, to coordinate liaison on studies being made at the two universities on the strength of welded steel frames and their components. The announcement was made recently by Dr. Martin D. Whitaker, president of Lehigh.

The trip will be under the sponsorship of the Welding Research Council and the Office of Naval Research in cooperation with the American Institute of Steel Construction, the American Iron and Steel Institute and the Lehigh University Institute of Research. Mr. Beedle is expected to stay at Cambridge University for three months.

Purpose of the trip is to promote closer liaison between the investigation of structural members at the Fritz Laboratory at Lehigh University and the study made by Prof. John F. Baker of Cambridge on the plastic behavior of welded rigid frames.

Lincoln Foundation Announces 1949–50 Engineering Undergraduate Award and Scholarship Competition

The third annual competition of the Engineering Undergraduate Award and Scholarship Program was announced recently by The James F. Lincoln Arc Welding Foundation of Cleveland, Ohio. This program offers annually $6750 in awards and scholarship funds to engineering undergraduate students and to schools for the best papers prepared by undergraduates on arc-welded design, research, fabrication or maintenance. A total of 77 awards ranging from $25 to $1000 will be awarded to students and $1750 for 7 scholarships will be awarded to schools.

The Foundation's Undergraduate Program, whose rules were formulated with the advice and help of 11 prominent college engineering deans, is conducted to encourage engineering students to investigate some phase of the science of arc welding. Regardless of previous knowledge of arc welding, all resident engineering undergraduate students, including agricultural and architectural, registered in a school or college in the United States are able to compete in the writing of papers for awards.

Student papers may have a wide variety of subject matter and can be based on study or actual experience. Papers may describe the welded design of a machine or structure or any part of a machine or structure. Maintenance and repair of machinery or farm equipment is another subject as well as any phase of welding fabrication. Laboratory research and development work may also be described. Awards to students for the best papers submitted will be made as follows:

Number of awards	*Amount of each award*	*Total amount of awards*
1	$1000	$1000
1	500	500
1	250	250
4	150	600
8	100	800
12	50	600
50	25	1250
77		$5000

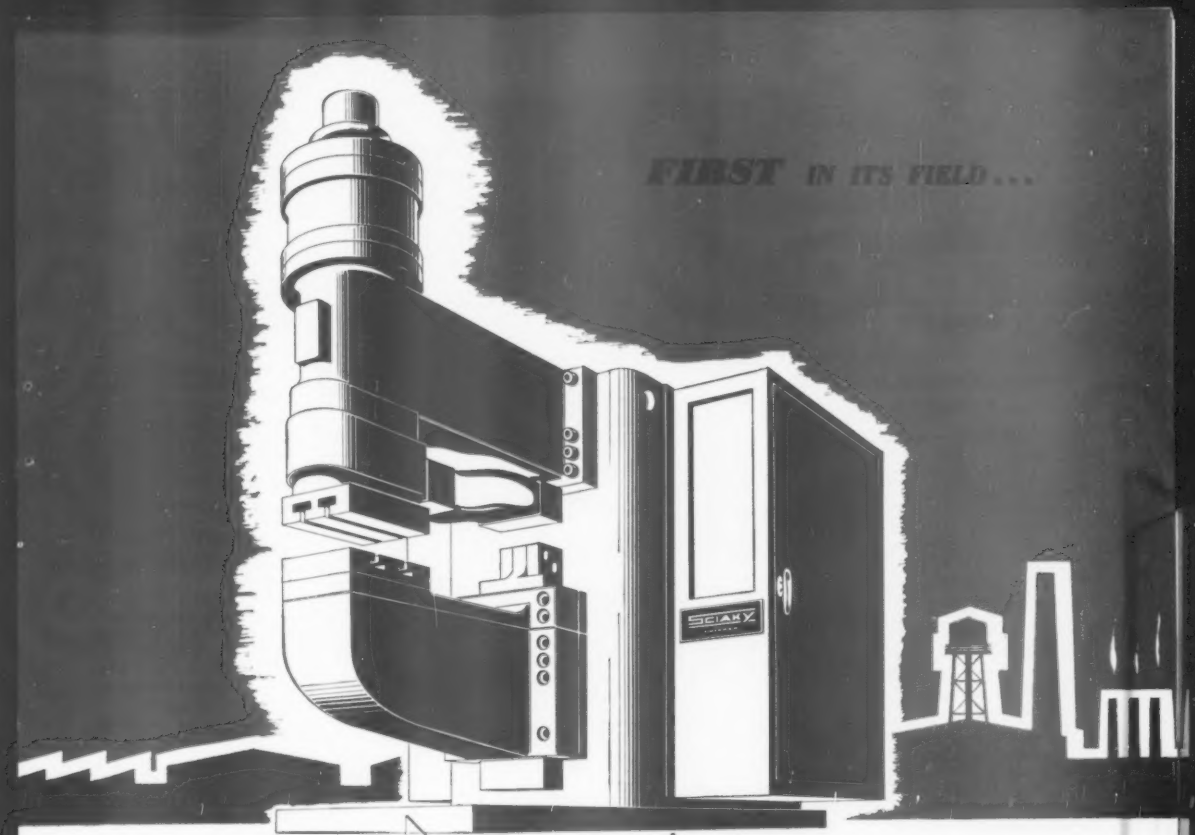
Scholarship funds will be awarded to schools according to the following plan: The schools or colleges in which the three top awards are made to students will receive amounts of money equal to those awards. These amounts will be used for scholarships in the departments in which the award-winning student is registered.

Number of scholarships	*Amount of each scholarship*	*Total amount of scholarship funds*
4	$250	$1000
2	250	500
1	250	250
7		$1750

As a result of previous competitions, a total of $3500 in scholarship funds has been awarded to the University of Cincinnati, University of Minnesota, University of Wisconsin, Case Institute of Technology and Iowa State College.

Dr. E. E. Dreese, chairman of the Board of Trustees of the Foundation, describes the purpose of this series of annual programs as the encouragement of the study of the science, technology and application of arc welding through the preparation of papers by students of engineering upon whose shoulders rests the responsibility for future industrial progress.

The Rules Committee which formulated the rules consisted of the Dean of Engi-



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neering at A & M College of Texas, University of Michigan, Pennsylvania State College, Cornell University, Case Institute of Technology, Carnegie Institute of Technology, Ohio State University, Massachusetts Institute of Technology, Georgia School of Technology, Polytechnic Institute of Brooklyn and Stanford University with Dr. E. E. Dreese of Ohio State University as chairman.

A copy of the Rules and Conditions may be obtained by writing The James F. Lincoln Arc Welding Foundation, Cleveland 1, Ohio.

Weldaloy Products Retains Tebben Co.

The Weldaloy Products Co. of Cleveland, Ohio, are retaining The John D. Tebben Co., Detroit, Mich., as sales consultants for the Weldaloy line of resistance-welding products.

James O'Grady, President of Weldaloy, in making the announcement stated, "We think we can do a more thorough job of coverage and meet the specific needs of users of resistance welding in industry."

Mr. Tebben, who is a former Vice-President of the AMERICAN WELDING SOCIETY and past chairman of the Detroit and Indiana Sections, will handle the assignment himself.

The Company is rounding out its line generally, adding several new products and is revitalizing their sales program by adding representatives in specific territories throughout the country.

John D. Tebben

The Weldaloy Co., has shown a consistently steady growth since their formation a few years ago. The Company maintains their own laboratories and specializes in metallurgical research. This department is under the direction of Robert "Bob" Shroder a nationally recognized expert in this field. For many years he was associated with Dr. William Hess at the Rensselaer Institute.

Blueweld Represents Reed Engineering Co.

Reed Engineering Co., of Carthage, Mo., announce the appointment of Blueweld, Inc., Milwaukee 4, Wis., as representative in the Wisconsin and Minnesota area for the Reed line of turning rolls and welding jigs for use with automatic welding.

Blueweld, Inc., was organized to serve welderies with technical assistance in the application of all electric-welding processes. The company was started in 1947 by its president, Fred C. Archer, who previously served Lincoln Electric Co. for 15 yr. as District Manager and Welding Engineer.

Resistance Welding Institute Moves Headquarters

Headquarters of the Resistance Welding Institute have been moved to the Hartman Bldg., Warren Rd. at Detroit Ave., in Cleveland, it was announced recently by Lee H. Judge, Director.

The Institute is an educational organization established earlier this year by the Resistance Welder Mfrs. Assn. to serve as a clearing house for information on the resistance welding processes.

Dockson's No. 1250 Turretype Arc Welding Helmet shown here.

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You get service-plus from DOCKSON "Better Built" Welding and Safety Equipment. Selected materials, experienced engineering and practical construction give you the BEST PERFORMANCE and MODERATE COST.

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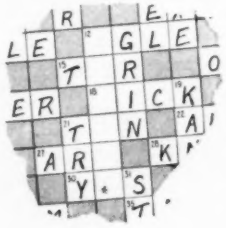

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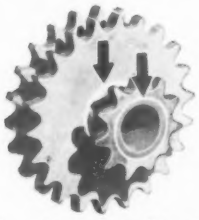
A simple way to DO something about ECONOMY-IN-PRODUCTION

EASY-FLO brazing this motorcycle handlebar instead of welding it saved 8¢ per assembly—a saving that grows big when you multiply it by thousands of assemblies.

Everybody is talking about economy-in-production. It's the keynote of the Metal Show to be held in Cleveland, October 17 to 21. But what counts most is *doing* something about it. In some cases, of course, that's not always so easy. There are, however, hundreds of fabricating operations where economy could be quickly effected by using methods which can be readily adopted.

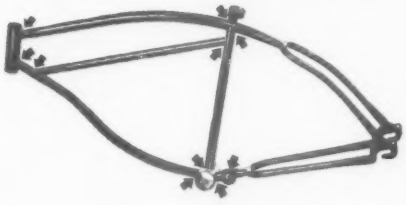
One such method that offers quick results is *EASY-FLO* and *SIL-FOS low-temperature silver alloy brazing.* It has boosted production and cut costs on thousands of products. Illustrated are a few typical tried and proved ways to

BRAZE AND SAVE WITH EASY-FLO AND SIL-FOS



Big savings are possible on many parts now machined from the solid by changing to a built-up assembly brazed with EASY-FLO. The triple-sprocket above and hose nozzle adjusting part at right are good examples.

Plenty can be saved on many parts now cast, by changing to EASY-FLO or SIL-FOS brazed construction. The gas burner (above), once an iron casting, is now made of tubular steel members EASY-FLO brazed at about 50% lower cost.



Finishing is another operation on which EASY-FLO and SIL-FOS brazing can save plenty. The maker of the EASY-FLO brazed bike frame (above) says—"This method produces joints of great strength which are absolutely clean—free from excess brazing metal—making possible a better looking enamel finish."

Attaching spuds to many types of equipment can be done faster and cheaper with EASY-FLO and SIL-FOS brazing. Using EASY-FLO and induction heating, instead of soft soldering by hand, reduced cost on the automobile oil pan spud (left) from $11.00 to $4.50 per hundred.

DO THIS - NOW

Write today for Bulletins 12-A and 15. They'll give you full EASY-FLO and SIL-FOS details. To get down to brass tacks in a hurry, ask to have a representative call. No obligation.

IF YOU GO TO THE METAL SHOW

in Cleveland be sure to see the EASY-FLO and SIL-FOS working exhibit. Brazing experts always in attendance to answer your questions and discuss your metal joining problems. **BOOTH 337**

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Less "pickup" Less "splatter"

G-E slope control for resistance welders

Tip "pickup" in spot-welding aluminum and expulsion in projection welding is reduced with this new Slope Control developed at General Electric. It is a method of applying current in gradually increasing intensity that results in longer operation of electrodes without the frequent cleaning usually required.

Here, again, is evidence of the research continually conducted at General Electric to improve welding methods—to make the production of improved products easier and less expensive. Here are the answers to the questions we knew you'd ask about Slope Control.

Q. What is Slope Control?

A. It is supplementary equipment used with a standard synchronous or non-synchronous precision control panel that regulates the rate at which current rises to welding value, and is adjustable in terms of cycles to reach final current.

Q. What effect does this have on the finished weld, and is it slower?

A. Our tests have shown that there is less tendency for porosity, which means stronger welds, and that it does not appreciably slow the work. See the cross sections of welds made with Slope Control.

Q. Can I use it on my present G-E electronically controlled welder?

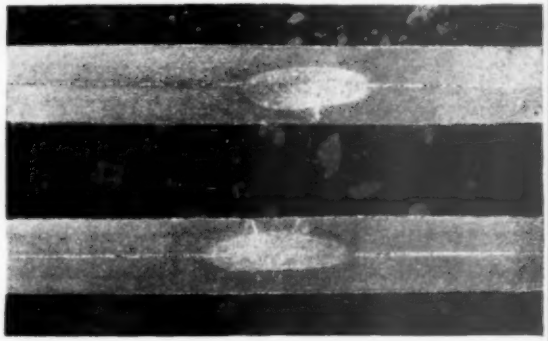
A. Yes. It is a separate panel that can be easily added to most modern G-E controls.

Q. With what types of welders and metals can it be used?

A. Use it for spot-welding aluminum (see the illustration), for projection welding aluminum or steel (it reduces expulsion), and for multiple projection welding, where you'll find improved results on non-uniform projections. It may be used with almost any metal, but aluminum and its alloys present a tough problem it can solve.

More details on G-E slope control in Bulletin GEC-534. Fill in and send in the coupon now. And when you have control problems for resistance welders, consult the nearest G-E sales engineer. He'll give you an unbiased answer based upon years of G-E experience with all types of welding. Whether it's a power factor problem, or the selection of the most suitable control for a specific job, you can be sure of your facts when they come from General Electric. *Apparatus Dept., General Electric Company, Schenectady 5, N. Y.*

Cross sections of aluminum spot welds produced with slope control (a microphotograph).



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Whether you're welding aluminum or heavy gage steel

...THIS IS FOR YOU

Projection welding with synchronous precision control. Slope control is mounted in side door of unit (small door open). Can also be used with standard 60-cycle spot welders having non-synchronous welding control.

Splatter reduced. Compare surfaces of these aluminum plates. Above, welds produced with single-phase, synchronous precision electric controls. That on left, with the addition of slope control.

ASK TO SEE "This is Resistance Welding," General Electric's More Power to America, four-color, sound motion picture covering every phase of this manufacturing process. Contains tips for improved production for operators of machines, design engineers, and supervisors. Contact your Resistance Welder manufacturer, Power Supplier, or the nearest G-E office.

Tip pick-up reduced. Electrode on left has been used 117 times with slope control. That at right used 41 times without slope control. Note wide area of aluminum pick-up.

General Electric Co., Sec. H645-50
Schenectady 5, N. Y.

Please send me Bulletin GEC-534 on Slope Control.

.....I would like a showing of "This is Resistance Welding."

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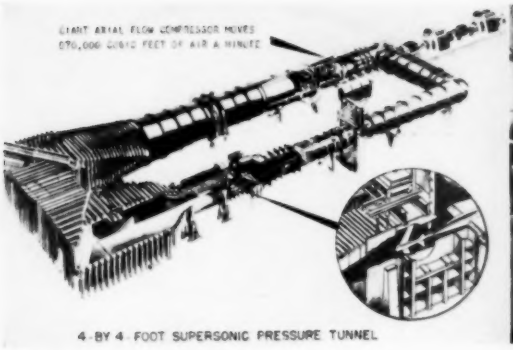


Fig. 1 Artist's sketch showing cutaway section views of 4- by 4-ft. supersonic pressure tunnel at the N.A.C.A. Langley Aeronautical Laboratory for studying problems of flight at speeds faster than sound

Fig. 2 Most important single element of the huge N.A.C.A. Langley Aeronautical Laboratory supersonic tunnel is this 870,000 cfm. axial flow compressor, designed and constructed largely by welding

Giant Wind Tunnel

First disclosure of operating details on the fastest of three giant tunnel installations for studying supersonic flight was made recently by the National Advisory Committee for Aeronautics and the Allis-Chalmers Mfg. Co. The 4- by 4-ft. tunnel has been in preliminary operation at Langley Aeronautical Laboratory in Virginia for the past year.

The new facility at the Langley Laboratory has design characteristics making it specially suited for the study of certain aspects of the over-all study of flight at speeds faster than sound. N.A.C.A. supersonic tunnels at the Ames Aeronautical Laboratory at Moffett Field, Calif., and at the Lewis Propulsion Laboratory at Cleveland, Ohio, are probing still other phases of the problem that is more important to aviation of tomorrow.

The Langley Tunnel permits the use of test models large enough for installation of extensive instrumentation. A 32-in. span model of a complete supersonic airplane now under investigation is fitted with movable controls and more than 300 pressure orifices.

Current tests are providing detailed information on viscous and interference effects, which were unobtainable in the smaller supersonic tunnels. Among other problems being investigated in the new tunnel at Langley is the phenomenon of shock-wave detachment at the leading edge of supersonic wings.

A key element of the new research unit is a specially designed axial compressor

CHAMPION "RAILEND" ELECTRODES

Fill the need for a Heavy Coated Electrode Specially Designed for Welding Battered RAIL ENDS

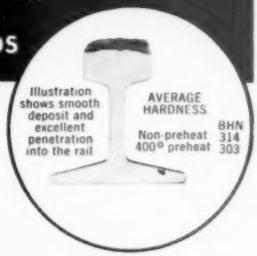
(Photo Courtesy of Erie RR)

"RAILEND" is available in 5/32", 3/16", and 1/4" diameters to meet all applications. The deposition of thin stringer beads is recommended for best results.

THIS ELECTRODE is outstanding in its class in that it produces dense, solid, smooth weld deposits which require a minimum amount of grinding to finish. Burn-off rate of "RAILEND" is very high which gives a maximum amount of deposit per man hour. Spatter is extremely low and the protective slag is easily removed.

This electrode is operable on both DC reverse polarity and on AC. It may be applied on rail in the range of outdoor temperatures of from 0° F. upward, or on rail preheated to 300 to 600° F. Preheating will insure uniform hardness of the deposited metal and guarantee crack free welds.

Penetration into the rail is excellent and a sound bond between rail and weld deposit is obtained.



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SIZE	AMPS	VOLTS
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3/16"	150-165	24-28
1/4"	210-260	28-32

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Smooth sound deposits can be made by both manual and automatic methods with HAYNES bare rods.

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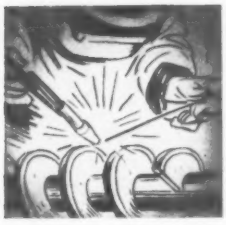

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This new process provides another fast method for depositing HAYNES bare rods.

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HAYNES STELLITE Alloy Grade 1: The hardest of the cobalt-base alloys recommended for coating surfaces subjected to extreme abrasive wear and slight impact. It is especially useful for resisting heat or corrosion combined with abrasion.

HAYNES STELLITE Alloy Grade 6: The toughest and strongest of the cobalt-base alloys. Recommended for coating surfaces subjected to mechanical or thermal shock. Produces sound deposits that will not check or crack.

HAYNES STELLITE Alloy Grade 12: A hard, wear-resistant alloy that is tough enough to withstand shock and impact. Recommended for coating large areas where deposits free from checks or cracks are required.

HAYNES Iron-Base Alloys

HAYNES Alloy 90: Highly wear-resistant. Resists oxidation and retains much of its original hardness at temperatures up to 1,000 deg. F. Withstands moderate impact.

HAYNES Alloy 92: A moderately low melting point rod. Use it on thin sections where low-temperature deposition is necessary to prevent distortion and on large parts when preheating is difficult or impossible. Deposits have a hardness of Rockwell C 65.

HAYNES Alloy 93: Gives excellent service where high cold-hardness is necessary. It is suitable for applications involving abrasion from sand, gravel, mill scale, or other hard particles.

HASCROME Rod: A work-hardening rod, recommended for use under conditions of severe impact. It is widely used for hard-facing rock and earth handling equipment parts. Recommended for electric application to manganese steel.

HAYNES Tungsten-Base Alloys

HAYSTELLITE Cast Tungsten Carbide: The hardest of all HAYNES hard-facing alloys, used where utmost resistance to abrasion is required. Typical applications include oil-well drilling tools, ditcher teeth, and coal undercutter bits.

HAYNES Nickel-Base Alloys

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Fig. 5 Disk is welded to stub end of axial compressor rotor —one of several special welding operations required in constructing the unique compressor unit for the N. A. C. A.

Fig. 6 After the second and sixth disks had been welded in place, supporting bars used to maintain proper alignment with the bearings of the heating unit were removed, and stub ends and center disks of the supersonic tunnel compressor rotor were strapped together preparatory to making final circumferential welds on the big spindle

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using Diversey No. 36 cleaner and Diversey No. 514 deoxidizer

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Yes, if you are looking for higher quality, increased production and lower costs with your aluminum alloy spot welding operation, investigate the Diversey Pre-Welding Treatment today!

Used by leading aircraft companies, case histories reveal that, by employing Diversey No. 36 to remove identification markings, grease, dirt; and by using Diversey No. 514 to remove oxide and heat scale, spot weld output increased up to 50%! Further, plants report an increase up to 4,000% in the number of spot welds now made before the electrode tips require redressing!

The Diversey Pre-Welding Treatment for Aluminum is easy, efficient, practical, and surprisingly economical to use! Mail the handy coupon today for complete information!

EASY AS A-B-C!

The cleaning efficiency of Diversey No. 36 is dramatically shown here! Before and after photos prove that Diversey No. 36 removes marking inks from aluminum surfaces easy as A-B-C!

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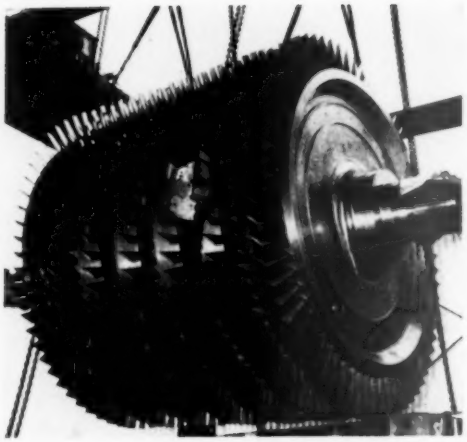


Fig. 3 Bladed welded rotor of the compressor weighs about 65 tons. Blades are 6 in. wide, from 9 to 14 in. long. Tip ends of the rotor blades travel at a speed of about 560 mph. when the unit is running at maximum speed

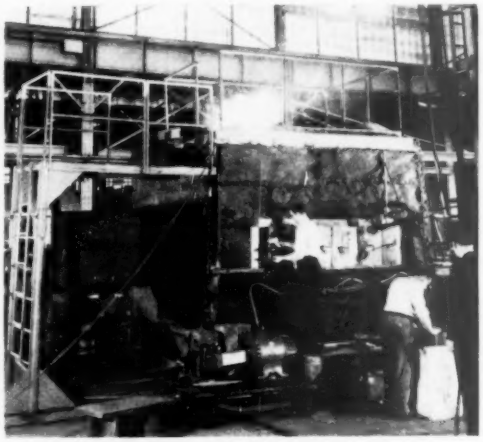


Fig. 4 Welding of the disk to make up the compressor rotor required a special heated and insulated housing to control temperature of parts. The heating unit provided for the continuous rotation of the rotor during heating, welding and final cooling; openings on the top, adequately protected from rising heat, for the actual welding; openings on the back for peening the weld and on the front for temperature and alignment checks during the welding. A special transit was used to check the relative position of grooves cut in the rings being welded together to check the alignment of the disks during the welding

which generates a flow up to 870,000 cu. ft. of air a minute and is believed to be the most efficient device ever built for the movement of air. It was designed and constructed by Allis-Chalmers in conjunction with N.A.C.A. Langley engineers.

The seven-stage, 11-ft. diameter, 1137-blade compressor operates at tunnel pressures from 0.25 to 2.5 times atmospheric pressure and requires up to 60,000 hp. to drive it. It circulates air through

Continued on page 1012

a new edition

of

Welding Metallurgy

First published in 1940, this book has had several printings and has been distributed all over the world. To bring up to date, Mr. G. E. Linnert has prepared an extensive revision so that the new edition has 505 pages—150 pages more than the first edition. Many new illustrations have been added to bring the total to 203.

Material has been added to cover the new processes such as inert gas metal-arc welding; material has been added to provide greater coverage of the stainless steels.

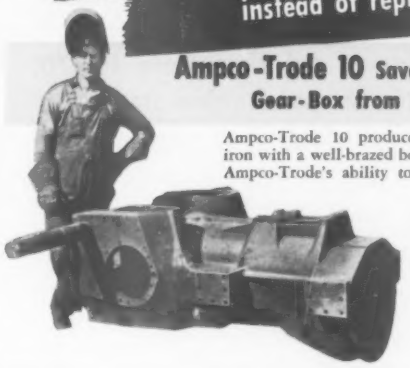
A list of books is given at the end of each chapter for those especially interested in the subject covered by that chapter. Questions for school use are grouped together at the end of the book; they can be used for home study as well.

$2.50 per copy. Order through American Welding Society, 33 West 39 Street, New York 18, N. Y.

For longer service at lower cost

...use Ampco-Weld repairs

instead of replacement parts...



Ampco-Trode 10 Saved this Costly Cast-Iron Gear-Box from the Scrap Heap

Ampco-Trode 10 produces dense, ductile welds in cast iron with a well-brazed bond and low base-metal dilution. Ampco-Trode's ability to weld cast-iron saved the cost of an expensive replacement when this complicated cast-iron gear-box broke into two pieces. Low-cost weld repair with Ampco-Trode 10 put it back in shape for extra years of service. *(Report from Montreal Welding Co., Montreal, Canada.)*

Ampco-Trode 300 Overlays Outlast Chrome Plate 4 to 1 on Draw Punches and Rings

Draw dies fabricated and/or rebuilt with Ampco-Trode 300 last 4 times as long! This more than offsets the lower initial cost of chrome plating. In addition, press-room efficiency gains nearly 15% by eliminating down-time formerly required to hone and polish dies in the middle of production runs! *(Report from Industrial Stamping and Mfg. Co., Detroit, Mich.)*

Phos-Trode Overlays Save nearly $4000 on a Single Turbine Repair Job

More than 20 years of cavitation and pitting had nearly destroyed the vanes and buckets on these 4-ton runners of a water turbine. Replacement would have cost more than $6000 — and there was a chance the original patterns were no longer available. The unit was completely rebuilt with Phos-Trode — restored to original contour — for 1/20th the cost of a replacement unit! *(Report from Montana Power Co., Great Falls, Montana.)*

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A. The Smith Dolan System can EASILY handle this problem of Preheating Stress Relieving and Normalizing to 1700° F on Carbon and Stainless Steels!

Boiler down-comer partially wrapped for stress relieving.

1. SAFE TO USE EASILY APPLIED ASSURED RESULTS	**Smith Dolan Heating Apparatus is so simple to handle that it can be operated by job personnel. Because it delivers consistently perfect results under safe conditions, it assures you of minimum job cost.**
2. THOROUGH PENETRATION	**It is no problem to achieve thorough heating of heavy sections with precise control in minimum time.**
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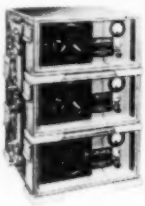
4. LABORATORY CONTROL UNDER FIELD CONDITIONS	**Smith Dolan Induction Heating Apparatus is designed and engineered with automatic or semi-automatic controls. Therefore, you obtain accurate, consistent results under field conditions, with a permanent record of each job always available.**



NEW MODEL U-P *— Patented Smith-Dolan System, portable, low frequency induction heater, three-high stack (shown) 30 kva—10 kva per unit. Buy one or stack 2 or 3 for increased capacity. Buy what you need —build as you go.*

INDUCTION CONTROL CABINET

used with Model U-P units (shown) and Model GC Duplex 120 or 150 kva induction heaters (shown in catalog).

ELECTRIC ARC **can supply equipment for ANY TYPE OF JOB—equipment that can be *purchased outright or rented*. From what other source could you possibly obtain such wide latitude in planning for every job in preheating and stress relieving before welding and normalizing? Today, more than ever, Electric Arc equipment is specified where other methods are too costly or impractical. Write for informative catalog.**

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Welding Equipment, Electrodes & Supplies

the test section of the tunnel at velocities ranging from 1.2 to 2.2 times the speed of sound.

Tremendous size of the tunnel compressor—it is 11 times larger in volume of air handled than the largest standard commercial compressor built to date—can be imagined from its total weight of 140 tons. Total weight of the blading is about 10 tons, the separately forged steel blades measuring 6 in. wide and from 9 to 14 in. in length. The bladed rotor weighs about 65 tons.

Tip ends of the rotor blades are approximately 11 ft. in diameter and travel at a speed of around 560 mph. when the unit is running at maximum speed. The 12½-ft. diameter compressor cylinder, which is an integral part of the wind tunnel, is split in halves. Each half is mounted on rails so it can be unbolted and moved back to facilitate internal inspection of the compressor.

Unusual size of the compressor rotor called for construction of seven forged steel disks and two forged steel shaft ends welded together. This was accomplished in a special heating unit with an insulated housing. The need for very accurate final balancing of the finished rotor required close machining of disks and shaft ends prior to welding and the prevention of warping during the welding operation.

Operating velocities in the tunnel's test section can be varied by adjusting the flexible 25-ft.-long walls of a nozzle ahead of the test section. By means of gates, the test section can be isolated to permit model changes without returning the entire tunnel to atmospheric pressure.

Drying and cooling equipment in the tunnel reduces moisture content of the air to as low as one part of water in 10,000 parts of air and stagnation temperature from 250 to about 110° F.

Letter to the Editor

August 18, 1949

To the Editor of THE WELDING JOURNAL

Sir:

I wish to take exception to an erroneous statement appearing in a press release on page 674 of the July WELDING JOURNAL. It is claimed there that a certain electrode (identified on p. 684 of the same issue as the G. E. Type W-1502 stainless electrode) complies with "A.S.T.M.-A.W.S. Spec. No. E-502," and yet the weld metal is claimed to contain 7.73% Cr.

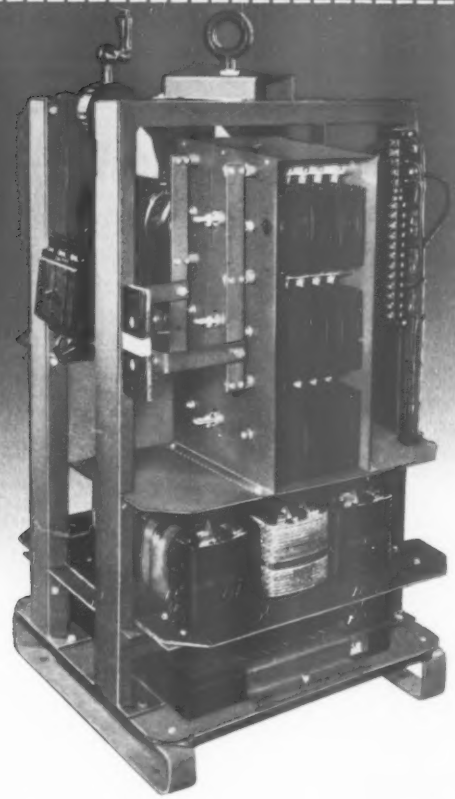
It is inconceivable to me that an electrode which deposits 7.73% Cr at 600 to 800° F. in a weld in 4.53% Cr pipe would deposit the required 4.0 to 6.0% Cr at 300 to 500° F. in a nondilution A.W.S. weld pad. I would expect the nondilution deposit to contain almost 9% Cr, which is not in compliance with the A.W.S. specification.

To my knowledge this error has been neither corrected nor commented upon by the manufacturer, although the release in question appeared in other publications as long ago as Apr. 11, 1949.

(Signed) Hallock C. Campbell
Associate Member, AMERICAN WELDING SOCIETY.

YOU CAN BE **SURE**... IF IT'S

Westinghouse



NEW!

Selenium Rectifier D-C Welder

SAVES POWER... OPERATORS LIKE ITS QUIETNESS

New D-C Welder replaces motor-generator set with Selenium Rectifier! Lighter... quieter... smaller... more efficient... higher power factor... less no-load losses. Available in 200, 300 and 400-ampere ratings.*

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* ***SEE*** *it...* ***USE*** *it at*
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Cleveland, Ohio... Oct. 17
Booth No. 2026

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EVERYTHING for Welding ANYTHING!

NEW LITERATURE

Zinc Coated Steels

A new, illustrated 12-page booklet, "Fabricating and Finishing of Armco Zinegrip-Paintgrip," describes recommended methods for forming, joining and painting this special zinc-coated and bonderized sheet steel.

Helpful suggestions for shop men are given in sections on storage, forming and drawing; welding and soldering; and cleaning and painting. To prevent surface damage and to maintain the paint-holding qualities of the special bonderized zinc coating, precautions and recommendations for welding and cleaning are discussed in detail. A description of the material and of paint-holding tests conducted by the National Bureau of Standards and Bell Telephone Co. laboratories are also included.

Since this zinc-coated steel can be formed and drawn like mild steel (the special coating does not peel or flake), most of the material in the booklet is limited to specific information on welding, cleaning and painting which will help operating departments do a good fabricating job.

Ampco News

The August issue of the *Ampco Welding News* is now available upon request. One article explains how $4800 was saved by repairing a turbine runner with Ampco-Trode electrodes.

Another tells the methods employed in salvaging a large cast-iron gear housing with Ampco-Trode. Many other interesting case histories are included in this issue. Write Ampco Metal, Inc., Dept., WN, Milwaukee 15, Wis.

Welding and Cutting Manual

Published by The Linde Air Products Co., a Unit of Union Carbide and Carbon Corp., 30 E. 42nd St., New York, N. Y., this manual consists of 208 pages, is 6 x 9 in. and costs $1.80.

This new handbook on the oxyacetylene process should be useful as a reference and instruction book for anyone who does welding and cutting. The style is simple and easy to read and instructions are given in step-by-step photographs of actual repair jobs.

While the book is written especially for the repairman, farmer, garage mechanic and maintenance man there are hints, short cuts and instruction material which will help any welding operator do a better job. For example, Chapter 10 shows over 100 time- and labor-saving things to make. There are detailed plans for making machinery guards, trailers, shop and kitchen stools, benches, tools and picnic grills.

Other chapters give short cuts and instructions for bronze welding; fusion welding; soldering; hard facing; cutting steel and cast iron; heating, forming and straightening metals; welding and cutting pipe; and welding nonferrous metals.

The appendix contains useful charts and tables, a complete glossary of welding terms, and a list of 100 repair jobs with recommended welding methods.

Regulators

Air Reduction, manufacturer of industrial gases and welding equipment, has announced the availability of a 32-page catalog covering its complete line of pressure regulators. This catalog is the third in a projected series of ten.

The catalog includes regulators for welding, cutting and special flame processes, for administering anesthetic gases, for maintaining gaseous pressures in electrical equipment and other operations where controlled gas pressure is required.

The catalog illustrates 26 regulators and describes over 100. There is a section covering regulator adapters and pressure gages. Also, 3 pages are devoted to flow and pressure charts.

For a free copy of this catalog, write the Airco sales office nearest you, or Air Reduction, 60 E. 42nd St., New York 17, N. Y.

Three Keys to Satisfaction

This is the title of a handsome brochure of 68 pages published by the Climax Molybdenum Co., 500 Fifth Ave., New York 18, N. Y., stressing the importance of design on the performance of any part. The subject matter is divided into three parts—design, steel and its treatment. The title indicates that all three factors have to be in good shape to result in satisfaction to the user. Copy available on request.

Lukenomics

An original, humorous and instructive pamphlet of 30 pages has been issued by the Lukens Steel Co., 503 Lukens Bldg., Coatesville, Pa., describing the products of the company and dealing especially with steel plates, clad steels, heads, plate shapes, welded components and completed machine. Copy of this booklet available on request.

Welded Steel Penstocks

The U. S. Department of the Interior has issued Engineering Monograph No. 3, "Welded Steel Penstocks—Design and Construction," by P. J. Bier of the Mechanical Engineering Division, Branch of Design and Construction.

This monograph presents information concerning modern design and construction methods for pressure vessels applied to penstocks for hydroelectric power plants. The data are based on some 20 yr. experience in penstock construction by the Bureau of Reclamation, during which period some of the largest penstocks in service today were designed and constructed.

The Engineering Monograph series was inaugurated to record developments, innovations and progress in the engineering and scientific techniques and practices employed by the Bureau of Reclamation in planning, designing, constructing and operating its structures and equipment.

Copy may be obtained from Bureau of Reclamation, United States Department of the Interior, Denver, Colo., at 25¢ per copy.

Hose

A new catalog section on its lines of welding hose has been published by The B. F. Goodrich Co., Akron, Ohio, and is now available upon request. The section illustrates and gives data on the company's Oxy-Acetylene and Duo-Weld hose as well as describing their construction.

Colmonoy Sprayweld Catalog

The Wall Colmonoy Corp. has published a new catalog describing the Colmonoy Spraywelder and Sprayweld Process. This 4-page booklet describes the Colmonoy Spraywelder, a powder metallizing gun, and the spraying of powdered Colmonoy No. 6, No. 5 and No. 4 alloys as well as other powdered metals, such as brass, copper, zinc, etc. It is pointed out that the Spraywelder can be used for metallizing as well as for the regular Colmonoy Process which combines both welding and metallizing. Several of the hard facing applications which have proved most adaptable to Spraywelding are also included. Numerous photographs illustrate the Spraywelder, the Sprayweld Process and a few applications. Wall Colmonoy Corp., 19345 John R St., Detroit 3, Mich.

Allis-Chalmers Motors

Latest construction features of Allis-Chalmers large direct-current motors, used in many industries especially those having applications requiring wide speed variation and fine speed control, are discussed in a new 40-page bulletin released by the company.

Included is a comprehensive description of "Frog-Leg" armature windings, a combination wave and lap winding that gives good commutation without cross-connectors, and views of the new style mill-type spherical seated thrust bearing with welded-steel pedestal for heavy service.

Copies of "Allis-Chalmers Large Direct-Current Motors and Control for Heavy Duty Drives," 05B6002A, are available upon request from Allis-Chalmers Mfg. Co., S. 70th St., Milwaukee, Wis.

Sight Conservation

A new brochure describing sight con-

"Welding cast iron is EASY with NI-ROD"

says Jim Bengough

"I've been welding for 20 years and never thought it possible to get an electrode to work as well on cast iron as Ni-Rod does."

WELDING A CREAM SEPARATOR WHEEL. With Ni-Rod electrodes, no preheating was necessary, so distortion was kept to a minimum. The wheel hub was welded, and broken gear teeth were built up.

FILING WELDED GEAR TEETH. After building up broken gear teeth with Ni-Rod, only hand filing was required to put the wheel into usable condition.

HAY PRESS BULL PINION SAVED BY NI-ROD. A broken section was built up with Ni-Rod and ground to shape after welding.

Experienced welders—like Jim Bengough of Hensall, Ontario—recommend Ni-Rod for welding cast iron because Ni-Rod is both dependable and easy to use.

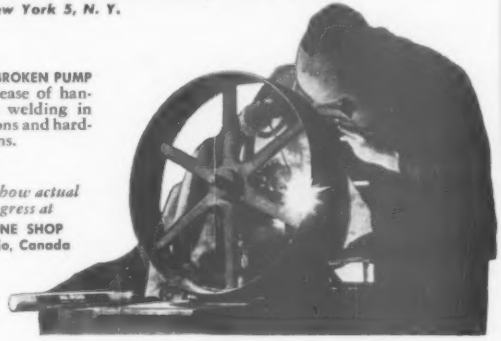
Serious breakdowns in farm machinery often call for welding cast iron parts . . . frames, broken gear teeth, wheel spokes, hitch links. With Ni-Rod* on hand, you can get laid-up machines back on the job sooner, even if you are not an expert welder.

Ni-Rod gives a stable arc in any position on either AC or DC. Preheating and post heating are seldom necessary. And Ni-Rod gives you welds that are machinable without special tools or techniques. Hand filing is often all that is needed to put a Ni-Rod welded part back in service.

Be ready to make quick, *dependable* repairs by ordering a package of Ni-Rod today. Your nearest INCO distributor stocks Ni-Rod in 3/32", 1/8", 5/32", and 3/16" sizes.

For complete information about INCO's welding materials, send for the latest catalog. *Reg. U. S. Pat. Off.

THE INTERNATIONAL NICKEL COMPANY, INC.
67 Wall Street, New York 5, N. Y.



NI-ROD REPAIRS BROKEN PUMP JACK. Ni-Rod's ease of handling simplifies welding in awkward positions and hard-to-reach locations.

Photographs show actual jobs in progress at
JIM'S MACHINE SHOP
Hensall, Ontario, Canada

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Eagle Metals Company	Steel Sales Corporation	Williams and Company
Metal Goods Corporation		
Metal & Thermit Corporation	**National Cylinder Gas Company**	**Hollup Corporation**

servation programs in industry has just been published by American Optical Co., Southbridge, Mass. Titled "Improved Industrial Vision, A Bonus for Management, A Benefit for Employees," the publication features case histories and charts demonstrating the cost saving values of an industrial safety and visual efficiency program.

The brochure is available upon request to American Optical Co.'s Industrial Safety and Visual Efficiency Bureau, William T. Cameron, director.

The program, developed by American Optical in cooperation with industry and the ophthalmic professions, provides a rapid check of vision under the supervision of members of the medical or optometric professions. Industrial employees who could benefit by a complete visual examination and correction for maximum safety and efficiency are thus located and informed of their need.

Surveys of programs inaugurated in a variety of industries were condensed for publication by American Optical Co. Large and small plants are represented in this report and, aside from the obvious humanitarian values of the programs, participating industries were in agreement that vision conservation is a definite method of cutting operating expense.

Prevention of eye accidents is an important aspect of the program and, through visual improvement, production has been increased in efficiency. Other less direct benefits such as improved employee morale have been noted by those adopting the AO program.

Tool Engineers Handbook

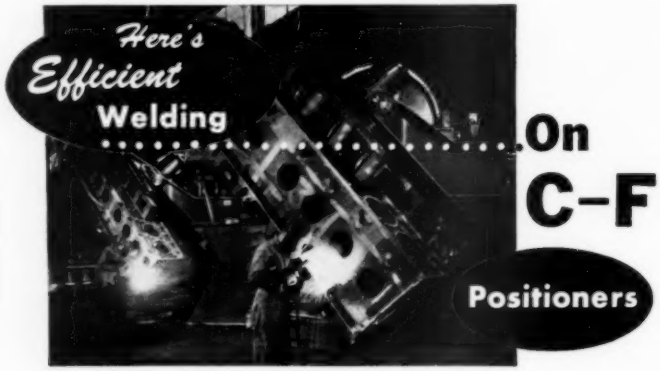
The *Tool Engineers Handbook*, American Society of Tool Engineers, is a truly monumental reference volume covering all phases of planning, control, design, tooling and other operations involved in the mechanical processing of finished products. Prepared with your specific needs in mind, it brings you basic factual data collected from all responsible sources—the dependable answers you need in making or affecting every variety of tool-engineering decision.

The material included is based, wherever possible and practicable, on published governmental, associational and industrial company standards. It is accurate and up to date. Right up to publication, scores of expert reviewers checked and rechecked the definitions, symbols, equations, tables, charts, methods, practices and procedures that make this handbook the most useful reference book in its field.

Here are just a few of the Handbook's outstanding features: (1) General topics have been split into smaller units for the most convenient reference and use. For example, instead of lumping all gear-manufacturing methods into one section, the material appears in six separate sections and subsections: Gear Milling and Hobbing; Gear Shaping; Bevel Gear Manufacture; Gear Finishing; Gear Design; Gear Inspection. Thus you can instantly find accurate assembled data on the single subject with which you are concerned. (2) Unquestionably, the most complete, compact theory on cutting forces and machinability available. (3) The subjects of planning, materials, machines, design and operations have been clearly separated. (4) All new important techniques and processes have been covered. Conventional tables of feeds, speeds and cuts have been modernized to bring data in line with recent advances in today's high-speed and high-power machine tools, latest cutting materials and cutting practices. (5) Includes all established standards and well-settled tentative standards such as unified screw threads, twist drills single-point tools, etc.

In addition, the book makes available invaluable material drawn from the pooled experience of almost 18,000 A.S.T.E. members. Here are data taken from confidential files, including such things as sketches of jigs, fixtures, dies and successfully devised small tools. Graphs, drawings, diagrams and sheets taken from company manuals and known to be the most advanced in accuracy and method of presentation have likewise been incorporated. Much of this is material never before made publicly available.

Price $15. McGraw-Hill Book Co., Inc., 330 W. 42nd St., New York 18, N. Y.



When heavy, unwieldy weldments like these diesel crank cases can be quickly swung into any position so that every weld is made downhand —that's efficient welding!

Weldors spend more time welding—do better welding at lower cost when they work with C-F Positioners because these hand or power operated machines reduce positioning time to a minimum. Investigate the cost-saving advantages of C-F Positioners. They pay their way in any company

Write for Bulletin WP24, an illustrated circular detailing the specific advantages of C-F Positioners

Cullen-Friestedt Co., 1323 Kilbourn Ave., Chicago

CULLEN-FRIESTEDT CO., CHICAGO 23, ILL.

C-F positioned welds mean better, more economical welds

Armco Stainless Steels

A new 36-page booklet, "Heat Treating and Pickling of Armco Stainless Steels," published by Armco Steel Corp., Middletown, Ohio, presents comprehensive and up-to-date information on heat treating and pickling methods for stainless steels. Recommended procedures, based on practical experience and extensive work in Armco's Research Laboratories, are given in detail so that full advantage can be taken of the valuable properties of stainless steel.

Subjects discussed in the booklet are general practices and equipment for heat treating stainless; recommended heat treatments for the chromium-nickel, ferritic chromium and martensitic chromium (hardenable) types of stainless steel; surface hardening; pickling (scale removal) methods; and heat-treating problems and remedies.

Important new material on heat treating the stabilized grades, how heat treatment affects corrosion resistance and special procedures for heat treating stainless steel forgings are discussed in detail.

Extensive use of charts and graphs to summarize heat treatments and show the effect of variations in heat-treating procedure on the mechanical properties of stainless steels make this booklet a handy reference manual for design and processing engineers as well as production departments.

This is the fifth in a series of Armco booklets on the fabrication of stainless steels.

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ELECTRODES PAY THEIR WAY...

TAKE ADVANTAGE OF THESE VALUABLE ARCOS SERVICES

Laboratory Research

Quality Control

Field Service

Convenient Stocks

Technical Information

In our long association with stainless fabricators, we have learned that their first and last interest in electrodes is the production of sound weld metal. Arc characteristics, ease of slag removal, bead appearance are all plus qualities easily evaluated but sound welds *on every job* and *from every pound of electrodes* is vital to the user's reputation.

We have further learned, as manufacturers, that stainless electrodes of a quality which will produce sound weld metal *all the time* are achieved only through a considerable expenditure of technical skill in testing and retesting. Arcos electrodes which do not produce trouble-free welds under our strict quality control tests are rejected *before they reach our warehouse.* This extensive care to produce trouble-free electrodes is reflected in the price of Arcos electrodes.

The extensive care in the Arcos processing of electrodes costs more than conventional methods used in the manufacture of low priced steel electrodes but it pays out when it eliminates the costs of chipping and rewelding or failure in service.

The best informed buyers of stainless electrodes specify Arcos.

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PERSONNEL

Air Reduction Announces Research Department Changes

Frank J. Aschenbrenner and Earl C. Clark have been appointed assistant directors of research and engineering, according to an announcement made by Dr. George V. Slottman, Air Reduction director of research and engineering.

Simultaneously, it was disclosed that the technical relations department was transferred from research and engineering to the secretary's department. John J. Crowe, assistant vice-president and manager of the technical relations department, will report to J. D. Gunther, secretary.

Mr. Aschenbrenner will be in charge of the Air Reduction Murray Hill (New Jersey) laboratory. Mr. Clark has been placed in charge of the development and engineering groups.

Dr. Slottman's statement revealed that J. K. Hamilton, manager of apparatus research division, and Dr. G. B. Carpenter, manager of chemical research division, will report to Mr. Aschenbrenner.

F. P. McKegney, chief engineer, W. Wilkinson, manager of liquefaction division, and C. J. McFarlin, manager of process engineering division, will report to Mr. Clark. The statement also announces the appointment of Dr. L. I. Gilbertson as administrative manager in charge of business management, and L. M. von Wanckel as executive accountant.

It was also announced that Frederick C. Saacke has been appointed safety engineer of the Air Reduction organization. In addition, he will assist Mr. Crowe in technical relations.

R. H. Davies Appointed to Baker-Raulang Post

Robert H. Davies recently joined Baker-Raulang as manager of engineering, in which capacity he will have supervision of all engineering functions for the company. In the past, Mr. Davies was associated with the Lockheed Aircraft Corp. as production engineer; Northrop Aircraft, Inc., where he was in charge of all production and tooling methods for experimental and prototype airplanes helped develop the Heliare welding process for magnesium; Henry J. Kaiser Co. as plant engineer in the building of Kaiser's magnesium plant, the alloy plant, the ferrosilicon plant and the magnesium sand-casting foundry; and at Permanente, Calif., where he subsequently became superintendent and was in charge of production and plant development.

Prior to coming to Baker, Mr. Davies was with Lincoln Electric Co. for the past 5½ yr. and served as their representative in Washington., D.C., and as consulting engineer in charge of educational work. Mr. Davies is a graduate mechanical engineer of the University of Minnesota,

Robert H. Davies

and is an active member of the AMERICAN WELDING SOCIETY.

Dennis Made Sales Manager

Graver Tank & Mfg. Co., Inc., East Chicago, Ind., has announced the appointment of Harry A. Dennis as manager of sales of the Weldment Division in the Midwestern area. Mr. Dennis was formerly with Lukens Steel Co. and their By-Products and Lukenweld Divisions as assistant district manager of sales.

Starting in 1939 with the Lukens Company as a helper in the open hearth, Mr. Dennis later became a welding research engineer and for several years did welding service engineering work.

Harry A. Dennis

Mr. Dennis is a graduate of Pennsylvania State College with the degree of Bachelor of Science in Metallurgy. He is a member of the American Society for Metals and the AMERICAN WELDING SOCIETY.

Airco Research Laboratory Changes Announced

The following personnel changes were announced recently by F. J. Aschenbrenner, newly appointed Air Reduction assistant director of research and engineering.

J. K. Hamilton becomes manager of apparatus research division with H. O. Klinke as assistant manager. J. T. McKnight is appointed superintendent of production and services section, and T. J. Cholis becomes supervisor of patent section. These men are all on the staff of the Air Reduction research laboratory located in Murray Hill, N. J.

Edmonds Made Field Engineer

P. L. Edmonds, who has been associated with Arcos Corp. since 1944 in testing and field engineering work, has been transferred to the New England District Office and will be the Arcos Field Engineer in the Boston, Providence and Worcester areas. Mr. Edmonds began his work in these areas on Sept. 1, 1949, with headquarters in the Statler Building, Rm. 900, Boston, Mass.

Rosenthal Promoted

Dr. Daniel Rosenthal, Associated Professor of Engineering, University of California, Los Angeles (UCLA) has been recently promoted to the rank of Professor at that university. He joined the staff of UCLA in 1946, having previously been at M.I.T., where he had been with the Department of Metallurgy since 1941. Before coming to this country he was teaching at the University of Brussels, Belgium, where he got his degree. Since 1925 Dr. Rosenthal has been active in various fields of welding research, the strength of welded joints, heat flow in welding, physical metallurgy, residual stress and more recently the brittleness of welded joints.

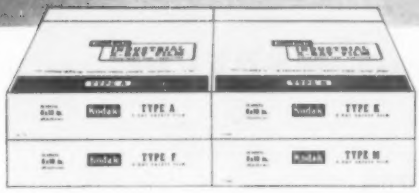
Fitzgibbons Boiler Co., Inc., Appoints Lange Sales Manager

Milton O. Lange, formerly Special Sales Representative for Joseph T. Ryerson & Son, Inc., New York, has been appointed Manager of Plate Product Sales for the Fitzgibbons Boiler Co., Inc., 101 Park Ave., New York City.

Mr. Lange will be in charge of planning and sales of special industrial steel-fabricated products such as condensers, air receivers, heat-transfer equipment, etc., in accordance with the recently expanded operations of Fitzgibbons in this field where a substantial volume of business has been carried on for many years.

WHAT'S THE RIGHT X-RAY FILM?

ANSWER:



KODAK INDUSTRIAL X-RAY FILM, TYPE A

● To examine such a dense and thick material the radiographer takes advantage of Kodak Industrial X-ray Film, Type A. For, in order to keep exposure reasonably short, the high speed of this film is essential. At the same time, its high contrast and fine graininess enable him to take full advantage of the ability of the 1000 kv machine to detect weld irregularities.

A TYPE OF FILM FOR EVERY PROBLEM

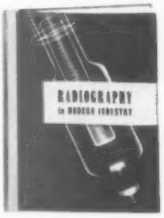
To provide the recording medium best suited to any combination of radiographic factors, Kodak produces four types of industrial x-ray film. They provide the means to check welds efficiently and thus extend the use of the welding process.

Type A—has high contrast with time-saving speed for study of light alloys at low voltage and for examining heavy parts at 1000 kv. Used direct or with lead-foil screens.

Type M—provides maximum radiographic sensitivity, under direct exposure or with lead-foil screens. It has extra-fine grain and, though speed is less than in Type A, it is adequate for light alloys at average kilovoltage and for much million-volt work.

Type F—provides the highest available speed and contrast when exposed with calcium tungstate intensifying screens. Has wide latitude with either x-rays or gamma rays, exposed directly or with lead screens.

Type K—has medium contrast with high speed. Designed for gamma ray and x-ray work where highest possible speed is needed at available kilovoltage without use of calcium tungstate screens.



RADIOGRAPHY IN MODERN INDUSTRY

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While with Joseph T. Ryerson & Son, Inc., Mr. Lange was responsible for the sale and production of numerous fabricated-steel products used in the construction of the George Washington Bridge, the Lincoln and Midtown Tunnels, the Eighth Avenue Subway and also T.C.C. units at the Tidewater Oil Co. and the Catalyst Bead plant for the Socony Vacuum Oil Co., both in New Jersey.

Reed Engineering Appoints Herrick

Clayton B. Herrick, former editor of the *Industry & Welding* magazine, has been appointed Ohio representative of the Reed Engineering Co. of Carthage, Mo., according to Lloyd H. Knost, General Manager of the company.

Herrick has just formed the C. B. Herrick Co. to handle the Reed line of power turning rolls, plate bending rolls and automatic welding fixtures.

During the war Herrick served as a welding supervisor in a shipyard, welding engineer in the Navy Department (Bureau of Ships) and as a member of the 70th Naval Construction Battalion (Seabees). After the war he became editor of *Industry & Welding*, which position he has recently resigned. He has written numerous articles on welding and related subjects and has compiled a Pocket Manual on Arc Welding.

Mr. Herrick first identified himself with the welding industry in 1934 when he joined the Lincoln Electric Co. Over a period of nine years he served them in production, engineering and sales capacities.

OBITUARY

W. M. H. Ballantyne

Will M. H. Ballantyne, Inspection Engineer for the Pennsylvania Water & Power Co., died on August 29th in Baltimore, Md., after an illness of several months. Born in Glasgow, Scotland, 63 years ago, Mr. Ballantyne attended Dumfries Academy and the Technical College of the same city.

After a short period of employment with Kelvin & James White, Ltd. in Glasgow, dan Everett Edgecomb & Co., Ltd., of London, he went to Canada in 1910 and took a position with the Canadian General Electric Co., of Toronto.

Mr. Ballantyne became identified with the Pennsylvania Water & Power Co. in May 1911 as an electrical inspector at the Holtwood hydroelectric plant on the Susquehanna River—at that time the largest hydroelectric plant in this country. He assisted in putting this plant in operation and in the years that followed took an increasingly active part in supervising the installation of equipment as additional units were placed in service. When the Safe Harbor hydroelectric development was started, his duties were enlarged to include like responsibilities for the Safe Harbor Water Power Corp. During the early part of World War II, he acted as technical consultant for The Eastern Rolling Mill Co., of Baltimore, Md., in connection with the installation and operation of equipment for manufacturing munitions.

Mr. Ballantyne was a member of the American Institute of Electrical Engineers, the AMERICAN WELDING SOCIETY and the Engineers Club of Baltimore.

Frank A. Conrad

Frank A. Conrad, vice-president of the Puget Sound Sheet Metal Works, Seattle, Wash., passed away on June 24, 1949, at the age of 52. He was an active member of the Puget Sound Section of the AMERICAN WELDING SOCIETY since its inception and was one of the early advocates of welding in the Pacific Northwest. His activities in this direction will remain as a monument to his vision and energy on behalf of welding. He was a member of the Executive Committee of the Puget Sound Section.

Mr. Conrad is survived by his wife, a son and daughter.

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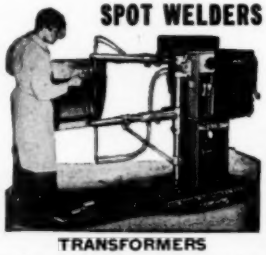
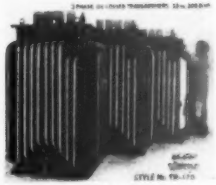
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TUBE-TURN

WELDING FITTINGS AND FLANGES

18-inch standard weight Tube-Turn welding tee and pipe laid out preparatory to assembly for hydrostatic pressure test.

Welder completing last bead in assembly. Fitting and pipe now form homogeneous, leakproof unit.

Assembly is hung in pit, massively lined with concrete. During test top grating is closed and held down by a barricade of sandbags.

Final result after hydrostatic pressure test. The straight pipe burst first, and the tee is undamaged.

Strength through Design in Tube-Turn welding fittings

The pipe burst first!

Take an 18-inch standard weight welding tee. Take three corresponding lengths of pipe. Weld tee and pipe together. Cap the ends. Lower the assembly into a pit. Then apply hydrostatic pressure—much more than the assembly is supposed to take!

Something has to give! According to the accepted beliefs it should be the tee. But not in the case of the test pictured here. The Tube-Turn "barrel-shaped" tee was used—and the pipe burst, well before the tee was affected! Throughout numerous tests, this new type of tee has withstood at least 25 per cent more pressure than required by the formula given in ASA B16.9.

The superior strength of the tee was achieved without running up extra weight and cost, by carefully planned improvements in shape (based on the sphere, nature's strongest form for internal pressure), and by a carefully engineered distribution of metal.

Tube-Turn "barrel-shaped" welding tee

This is a typical example of *strength through design* in Tube-Turn welding fittings, and another good reason why leading piping engineers specify Tube-Turn equipped, welded piping.

TUBE TURNS, INC.

222 East Broadway, Dept. K, Louisville 1, Kentucky

District Offices at New York, Philadelphia, Pittsburgh, Chicago, Houston, Tulsa, San Francisco, Los Angeles

NEW PRODUCTS

Rotating Vise

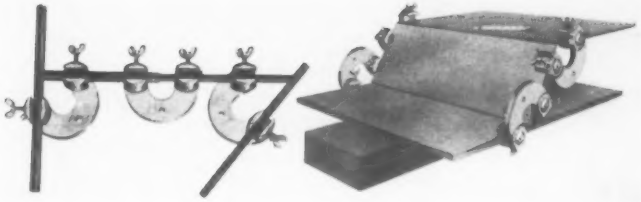
An addition to the versatile equipment [illegible] is a rotating vise [illegible] can be easily [illegible] the [illegible] (Chicago Welding Supply Co., [illegible] Chicago, Ill.).

The addition of Stoody Electrode [illegible] of Stoody welding [illegible] manganese steels [illegible] Stoody Co., [illegible] Calif.).

Electrode for Joining Manganese Steels

Jetweld (10) manufactured by Jetweld Welding Rods Corp., [illegible] positive arc for welding manganese steels without preheating. It deposits high tensile and low [illegible] of 1.5-2% manganese steel and [illegible] tensile strength of deposit is [illegible] and [illegible] resistance [illegible] for hot welding between or [illegible] sliding, sparks and heavy [illegible] equipment.

Jetweld is the first [illegible] to provide resistance with maximum [illegible] Jetweld (10) is now available in [illegible] and [illegible] Jetweld Electrode [illegible] is [illegible] and [illegible] [illegible] with [illegible] and [illegible] [illegible] [illegible] with [illegible] [illegible] [illegible]



By the use of Pro-Clamps, piping systems and weldments can be completely installed and assembled before the first weld is applied. Special fixtures and templates are completely eliminated in a high percentage of cases and considerable labor is saved for the reason that the fitters and assemblers are not standing idle while the welding operator applies tack welds and, visa versa, the welding operator is not idle while the fitters are making measurements and moving other parts into place.

Pro-Clamps consist of a U-shaped frame that can be likened to a pair of arms. At the end of the arms, two clamps which can be compared to a pair of mechanical hands are provided for gripping the parts which are held in place for welding. So that the clamps will hold the workpieces at any conceivable angle they are rotatably mounted and can be preset at any angle from 0 to 180°. The frame of the tool is inscribed with two accurate protractor scales so that the setting of the angle will be exact. A means is provided for very tightly locking the clamps at the preset angle.

Pro-Clamps are constructed of light weight, unusually high-strength alloy, which resists weld spatter.

The present model accommodates thickness and diameter from 0 to 1 in. Over-all dimensions are $5^1/_2$ x 8 in. Weight is 3 lb.

For additional information contact Bernard Welding Equipment Co., 741–43 E. 71st St., Chicago 19, Ill.

Miniature Inert-Arc Electrode Holder

A miniature inert-arc electrode holder, featuring a flexible front-end assembly made of malleable copper tubing surrounded by a sheath of silicone rubber so that it can be bent in any direction to reach hard-to-get-at places, has been announced by General Electric's Welding Divisions.

Specifically designed for the fluxless welding of nonferrous metals in the thinner gages from No. 16 to No. 40 (0.0625 to 0.003 in.), the holder is available in two models: One for 0.010- and 0.020-in. tungsten electrodes and the other for 0.040- and $^1/_{16}$-in. tungsten electrodes. It has a rating of 40 amp. continuous, and can be used with either a.-c. or d.-c. supply. Currents as high as 60 amp. can be employed, if overheating is prevented by very low duty cycles.

Small, light and extremely adaptable, the new welding tool will find application in the manufacture and repair of surgical instruments, cutlery, business machines, control and measurement equipment, capillary tubing, electronic tubes, duct work, wire fittings, small sheet metal enclosures, metal novelties, etc. It should prove useful to manufacturing jewelers and in model building and laboratory apparatus construction. Some of the metals with which the new holder can be used are stainless steel, aluminum, brass, copper, nickel, Monel, Inconel, gold, silver and tantalum.

Leak Detector

All fuel and traction tanks produced by the Lintern Corp. of Berea, Ohio, are now being tested with a General Electric Type H leak detector, it was learned recently.

The completely welded fuel tanks are made for use in large transportation trucks and have capacities ranging from 85 to 150 gal. To make certain there are no possible places for gasoline to seep out, all welds are checked by the portable G-E detector before the finished tanks pass from the assembly line.

According to Lintern engineers, six welders and one inspector armed with the leak detector can turn out about 18 tanks in the average day. Fabrication is in six stages. The tanks are placed on iron rails rather than positioners so that they can be pushed from one welding booth to another as they move along the assembly line. The top plates of the tanks are made of mild steel deck plate; the bottoms and ends are 12-gage sheet steel. First, the inner baffles and braces and the bottom sections are tacked together, the side panels are then tacked on, and final weldments are made after all the parts are assembled. The weldments measure about $^1/_8$ in. Sixty feet of welding electrode goes into each tank.

When the finished tanks reach the end of the assembly line, the inspector checks all welded seams with the Type H leak detector. This electronic instrument is so sensitive that it can detect leaks small enough to release only 1 oz. of gas a century. Previous leak testing methods were unsatisfactory, Lintern engineers said, because they uncovered only the large leaks. The new instrument enables the company to detect the smallest of leaks in

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WELDING CONNECTORS
Saxe System Welded Connection Units
for welded assembly

Saxe Units place in position and securely hold together structural parts to be welded.

As used in many welded structures they *eliminate all hole punching* producing an economical, rigid, safe and quickly erected structural frame.

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a tank and these can be repaired before the unit is shipped, they said.

D.-C. Welder Uses Plate-Type Rectifiers

New d.-c. welding machines, using plate-type rectifiers instead of the conventional rotating components used in motor-generator welding equipment, are available from Westinghouse Electric Corp. These welders are rated in accordance with N.E.M.A. standards for industrial type, single-operator arc welders. They are available in 200-, 300- and 400- amp. ratings.

Fig. 1 New Westinghouse d.-c. welder plate-type rectifiers

Major components of the new rectifier-type, d.-c. welders are: three-phase welding transformer; three-phase adjustable reactor; and a plate-type (selenium), three-phase, full-wave rectifier.

The no-load loss in a 300-amp. welder is 500 w., as compared to 2480 to 3600 w. for a conventional motor-generator welder.

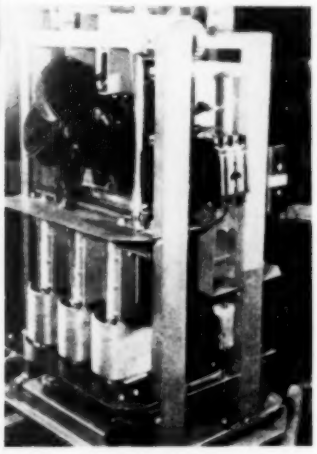

Fig. 2 A view of the welder, case removed, shows the exhaust fan, powered by a 1/12-hp., prelubricated ball bearing, totally enclosed motor

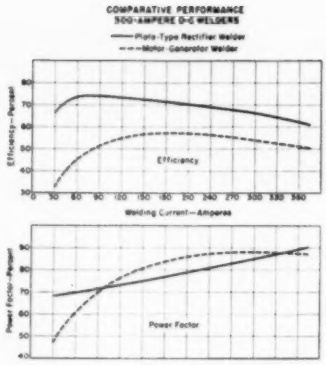

Fig. 3

The power factor at normal operating load conditions is comparable to induction-motor-driven welders. The welder's efficiency at full load is 66%, as compared to 54% for motor-generator welders. Efficiency increases at reduced load conditions, reaching 73% at 20% rated load.

For further information write Westinghouse Electric Corp., P. O. Box 868, Pittsburgh 30, Pa.

Self-Propelled Arc Welder

This fleet vehicle was especially designed as a mobile unit to quickly and efficiently make welded repairs around large railroad yards, oil refineries, steel mills, quarries, gravel pits and other industries having extensive operations.

Weldmobile's equipment and design are so wide in range of capabilities that it can be used as a complete maintenance and repair outfit in any location, eliminating the need of electrical connections for electric drive arc welders or towing equipment for trailer mounted arc welders. Two new models are now being produced, the GR-301-M with a 300-amp. welder for light to heavy welding requirements, and the GR-401-M with a 400-amp. welder for medium to extra heavy welding requirements. Either unit can be furnished with 1- or 3-kw. auxiliary d.-c. power for supplying lights and universal power tools.

This "Weldmobile" is equipped to roll along to the job under its own power and make on-the-spot repairs with arc welding, oxyacetylene welding and cutting, and power tools. A universal coupler is provided on the rear to permit towing of additional equipment such as trailer mounted welders, air compressors, etc. Manufactured by Hobart Brothers Co., Box 389, Troy, Ohio.

SR-4 Strain Indicator

A new SR-4® self-balancing, dial-type strain indicator for use in stress analysis with SR-4 bonded resistance wire strain gages, is announced by The Baldwin Locomotive Works, Philadelphia 42, Pa. The automatic balancing feature simplifies strain measurement by giving quick indications of strains on a dial scale calibrated to read directly in micro-inches per inch for standard gages (gage factors from 1.90 to 2.20). Adjustments for gage factor and for zero setting are provided above the dial inside the case.

The indicator can be used either with a single strain gage or with multipoint

The new Baldwin SR-4 self-balancing dial-type strain indicator has a 10-in. diameter scale. This model is designed for three ranges. All adjustment and selection controls are readily accessible when door is open as shown

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Cleveland will be crowded during the METAL SHOW so make plans and secure hotel accommodations before it is too late.

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National Metal Exposition
7301 Euclid Avenue, Cleveland 3, Ohio
or telephone UTah 1-0200

For hotel reservations, write:
Cleveland Housing Bureau
attention: Louise D. Perkins,
511 Terminal Tower, Cleveland 13, Ohio

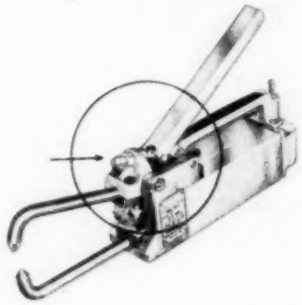
switching and balancing units for reading many strain points. It is available with any one of three ranges, with any two or with all three ranges: 0 to 2000, 0 to 5000 and 0 to 10,000 microinches of strain per inch of strained surface. Scales other than that illustrated are available.

Accuracy of the indicator is high and permanent. It is guaranteed within 0.25% of all ranges and is unaffected by the normal variations in the characteristics of electronic tubes used in the detecting and amplifying circuit, or by normal variations of voltage in the a.-c. power supply (110 v., 60 cycle).

Strain recording equipment of the same ranges and accuracy is also available.

Portable Spot Welder

The Miller Electric Mfg. Co., of Appleton, Wisc., has announced a redesigned, improved, portable spot welder with a new toggle-action tong lever.



Portable Spot Welder

The new type toggle-action tong lever gives greatly increased tong pressure resulting in better spots and easy operation. With ordinary lever-type spot welders where material fit-up is poor, sufficient hand pressure cannot be exerted to pull the material together. The toggle-action lever eliminates this difficulty. The lever may be adjusted to either close tight on the work or to pivot past center and lock tight on the work. Thereby, the operator can close the welder on the material to be spotted—forcing it up tight and locking together—then operate the switch with the other hand. The design assures uniform pressure on every spot and results in high-quality welds. It does not take muscle to operate this new model. Even women can exert satisfactory pressure with this new type of lever.

An adjustment is provided between the lever and the tongs to compensate for various thicknesses of metal. At the rear of the handle another adjustment regulates the distance the handle closes.

This new model spot welder may be used either with or without the timer control.

A kit to provide the toggle-action tong lever action for older model portable spot welders is also available.

Illustrated literature and further information are available either through any Miller distributor or upon writing direct to the Miller factory.

Inexpensive Positioner

J. M. Ragle Industries, 4029 Indiana Ave., Kansas City 4, Mo., have seen a definite need for a small inexpensive positioning fixture for such welding jobs as hard facing plow shares and other welding jobs where the part must be positioned.

Inexpensive Positioner

With this in mind they developed the Commando Positioner. This positioner will hold parts up to 2 in. thick and weighing 200 lb. It is ideally suitable for plow share welding inasmuch as the holding fixture is of a 3-contact arrangement. Pressure is exerted in the opposite direction from that caused by the welding operation. This feature eliminates warpage of the part being welded to a minimum.

The positioner is of the ball and socket type, accurately machined. The part being welded may be turned in any position. Position may be changed with slight pressure with the hand or fingers. It may be mounted on bench or inexpensive stand which may be made in any shop.

Heavy-Duty Electrode Holder

Operating under continuous duty cycle at 725 amp. on $^{3}/_{8}$-in. electrodes, with the new Martin Wells heavy-duty 68 Electrode Holder, the manufacturer states his holder remains cool.

Silver-brazed cable connections and size 4/0 cable are recommended for use with this holder on 600–700 amp. Electrodes are held and fixed in a 60 or 90° position by cam bulldog action. Manufacturer states the cam action gives a positive contact on the electrode and reduces the heating on both the stub and the holder.

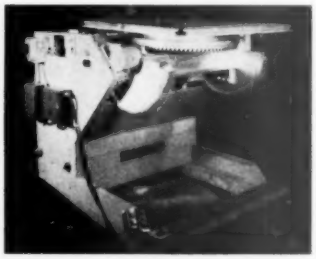
Length is 15 in., weight is 32 oz. Martin Wells, Inc., 5886 Compton Ave., Los Angeles 1, Calif.

Heavy-Duty Electrode Holder

Positioner

The latest manufactured product of the Aronson Machine Co., Arcade, N. Y., is a heavy-duty, gear-driven welding positioner that augments the Company's Universal Balanced Positioner line with capacities of 3000, 4000, 5000 and 6000 lb.

The four models of heavy-duty positioners are HD3000, HD4000, HD5000 and HD6000. Variable speed drive of the work table offers speeds of the machines infinitely variable from 0 to 0.75 rpm. on all models. The work tables also tilt 135° in 40 sec.



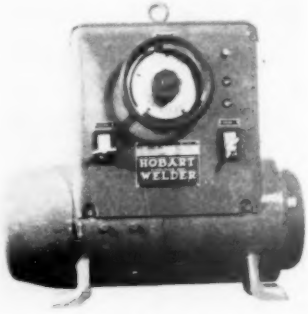
Positioner

Other features common to all models are enclosed motor, variable speed drive and speed reducer units with controls conveniently located on one side of the machine, remote push button, elevating base adjustable 24 in., and more length of tee slots for greater adaptability.

Low Cost D.-C. Welder

A new line of low-cost welders has been developed by the Hobart Brothers Co., Troy, Ohio, to be known as the "Bantam Champ" d.-c. arc welders.

The one shown is the electric-motor-driven Model MZ-200-S, designed to meet a certain demand for lower priced equipment. This model is rated 200 amp. at 30 v. on 50% duty cycle. The current range is from 40 to 250 amp. at an operating speed of 3450 rpm. It is 26¼ in. long, 18 in. wide, 25 in. high (stationary) and weighs approximately 340 lb.



Low-Cost D.-C. Welder

The generator is a modified multirange type with 4 laminated main poles and 4 interpoles which are removable. Four heavy-duty generator brushes are held in a fixed neutral position by patented single-unit brush rigging.

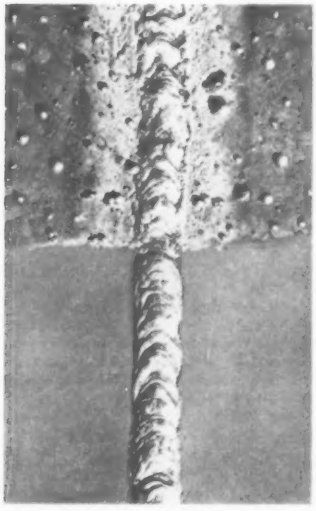
Its welding controls are modified multirange dual control, with 5 ranges of welding current and 100 steps of volt-ampere adjustment in each range, making available 500 combinations of open-circuit voltage and welding current for selecting any desired arc characteristics. Main switch is heavy copper, molded in bakelite, controlled by the large, hard-rubber covered hand wheel. Volt-ampere adjuster is compactly built behind the main switch.

A squirrel cage induction type electric motor is used and its rotor bars are welded to form a solid copper squirrel cage. No bolts or rivets are used in its construction. The stator is ground concentric with the bearings to insure a uniform air gap.

Voltage changes over terminals are provided on the starting switch. Motor leads are brought out to the terminals on the starting switch and can be re-connected to change from 220 to 440 v. power supply and vice versa.

Spatterproofing Compound and Rust-Preventive for Welding

Protect-O-Metal No. 8 is a new coating to protect parts and raw stock from rusting prior to welding and between operations. Applied with brush, spray or by dipping, it dries in less than an hour to a thin, transparent coating which prevents rust up to 6 mo. in the weather and up to 2 yr. on steel stored indoors. The coating also prevents adhesion of all flash and weld spatter during welding operations and saves up to 85% of the time required to clean welds. The coating will not cause porosity or harm the weld in any way and can be safely used on any metal surface.



Treated and Untreated Section of Welded Steel Plate

Protect-O-Metal No. 8 need not be removed after welding as it serves as a good base for subsequent painting.

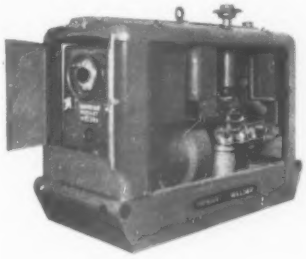
Protect-O-Metal No. 8 has also been found suitable in protecting beveled plate edges from rusting prior to welding and to protect overlapped sections which are inaccessible for painting after welding.

The product is delivered ready for use in 1- and 5-gal. cans and 55-gal. drums. Free test sample is available on request. Manufactured by G. W. Smith & Sons, Inc., 5400 Kemp Rd., Dayton, 3, Ohio.

The accompanying photograph contrasts one section of a welded steel plate which was left unprotected while the remaining section was treated with Protect-O-Metal No. 8 before welding.

Low-Cost Gas Drive D.-C. Welder

A new line of low-cost welders has been developed by the Hobart Brothers Co., Troy, Ohio, to be known as the "Bantam Champ" d.-c. arc welders.



Low-Cost Welder

The one shown is the gasoline-engine-driven Model ZXB-200-S, designed to meet a certain demand for lower priced equipment. This model is rated 200 amp. at 25 v. on 50% duty cycle. The current range is from 25 to 230 amp. at an operating speed of 2200 rpm. It is 55 in. long, 22 in. wide, 38 1/2 in. high (including exhaust), and weighs approximately 770 lb.

The generator is a modified multirange type with 4 laminated main poles and 4 interpoles which are removable. Four heavy-duty generator brushes are held in a fixed neutral position by patented single-unit brush rigging.

Its welding controls are modified multirange dual control, with 5 ranges of welding current and 100 steps of volt-ampere adjustment in each range, making available 500 combinations of open-circuit voltage and welding current for selecting any desired arc characteristics. Main switch is heavy copper, molded in bakelite, controlled by the large, hard-rubber covered hand wheel. Volt-ampere adjuster is compactly built behind the main switch.

The generator is driven by a Hercules ZXB, 4-cylinder industrial engine. It is water cooled and is complete with oil bath type oil filter, gasoline filter and muffler.

Graham Stud Welder

Graham Manufacturing Corp. announces the addition of a single gun machine to their line of welding equipment.

Graham Stud Welder

The basic principle of the Graham welder is a capacitor-operated, self-timed device using tip studs which, on coming in contact with the work and fusing, causes ionization; which, in turn, allows a path for the main discharge current of the capacitor to form an arc sufficient to melt both the full diameter of the stud end and the workpiece directly under same. This is followed by the necessary hammer blow causing the pieces to weld. This complete cycle is brought about by a rapid, continuous movement of the stud-holding part, no retarding means being employed. The time of arc is about 1 mil sec., which makes possible use of very high currents.

Some of the advantages of the extremely short arc time are the concentration of heat, the welding of studs on very thin metal, no fillets, no distortion of work, no discernible heat, allows studs to be welded on back of plated or painted surfaces without marring same, no flux is necessary and it makes possible welding of dissimilar metals.

Other advantages are low energy consumption, small size gun assembly, can be used in any position, only two moving parts in the gun and no skilled help required.

Some of the metals and alloys welded by this process are steel, stainless steel, Monel, aluminum, magnesium, zinc, copper and, in some cases, combinations of these metals and alloys.

Stud dimensions are up to 1/2 in. at present and the current rating is A. C. 110/220/440 three phase.

No timing devices are required. Foot pedal or switch can be used on manual cycle times.

The welding pressure can be either mechanical or pneumatic. No auxiliary cooling means are necessary. The overall dimensions of cabinet are 38 1/2 x 24 x 16 in.

abstracts of
CURRENT WELDING PATENTS

prepared by V. L. Oldham

printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 10, D. C.

2,477,117—Electric Welding Machine and Control Means Therefor— Irvin W. Cox, West Allis, and Lester D. Drugmand, Greenfield, Wis., assignors to Cutler-Hammer, Inc. Milwaukee, Wis.

The control means disclosed by this patent relate to a welding system utilizing a single unidirectional pulse of electric energy, and the apparatus provided includes a pair of relatively movable welding electrodes which have means associated therewith for pressing them together. These pressure producing means include an electromagnet having a coil in series with the electrodes and adapted to vary the pressure set up by the electrodes and controlled by the amount of magnetic flux induced in a portion of the electromagnet by the coil of same.

2,477,129—High-Frequency Induction Brazing Apparatus— Joel R. Johnson, Chicago, Ill., assignor to Western Electric Company, Inc., New York, N. Y.

Johnson's patent relates to a special fixture or apparatus for use in intermittently conveying articles to be treated to a high-frequency induction heating coil. A rotary article supporting table is provided and articles are carried thereon and are indexed with relation to an induction heating coil by movement of the table.

2,477,181—Composition and Method for Cleaning Aluminum Preparatory to Spot Welding— Emmette R. Holman, Pasadena, Calif., assignor to Turco Products, Inc., Los Angeles, Calif.

The method of this patent comprises immersing aluminum articles in a cold aqueous phosphoric acid bath having a pH below about 3 and an initial concentration of about 0.01 to 2.0%. This acid bath also has a substance therein capable of yielding a halide radical in it.

2,477,207—Shielded Electrode Holder— Fred J. Rinehart, Youngstown, Ohio.

This patent comprises a holder including a pair of handles pivoted to one another, portions of which form electrode engaging jaws. A bifurcated shield is affixed to the jaws adjacent their pivotal connection.

2,477,211—Welding System— Clyde E. Smith, Warren, Ohio, assignor to The Taylor-Winfield Corporation, Warren, Ohio.

Smith's welding system uses a multiple phase source of alternating current and it has a load circuit and translating means for furnishing the load circuit with electrical energy from each of the phases of the electrical energy source. The novelty of the device resides in the switching means used to connect and disconnect one phase of the source with the translating means and it includes a pair of reversely connected electric discharge devices for interconnecting another of the phases of the electrical energy source with the translating means.

2,477,582—Welding System— John W. Dawson, West Newton, Mass., assignor to Raytheon Mfg. Co., Newton, Mass.

This welding system is provided for welding two surfaces together and comprises providing means for holding the surfaces in spaced relationship to each other and other means are provided for connecting a source of arc current to such members for striking an arc between the surfaces to be welded to heat same to a high temperature below the drip point of the members. Other devices are present in the apparatus for bringing the surfaces into tight engagement and current flow is provided between the surfaces when they are in engagement.

2,477,622—Condenser Welding System— Hans Klemperer, Belmont, Mass., assignor to Raytheon Mfg. Company, Newton, Mass.

This patent is primarily directed to an electric control circuit for a welding system and such control includes a condenser and means for charging the condenser. A welding load circuit is connected to the condenser and the condenser is discharged through the welding load circuit with a unidirectional pulse of current. Means are provided for recharging the condenser with its original polarity by the energy stored in the system as a result of the pulse of current. Other special control means are provided in the circuit.

2,477,765—Stud for Welding— Ted Nelson, San Leandro, Calif., assignor to Morton Gregory Corporation.

Nelson's stud has a cylindrical head for welding to another body and it has a cylindrical shank for holding the head in the chuck of a welding implement. This shank has a conical neck frangibly connected to the head at its smallest end.

2,477,894—Method of and Apparatus for Forming and Welding Contacts— A. F. Pityo, Clifton, & H. E. Butterfield, Jr., Maplewood, N. J.

This patent covers a specialized apparatus of the type indicated wherein a metal rod is intermittently set by a step-by-step movement for securing contact sections from the metal rod to a metal member.

2,477,906—Apparatus for Electric Arc Welding— Nils Gustav Smith, Goteborg, Sweden, assignor to Elektriska Svetsningsaktiebolaget, Goteborg, Sweden.

Smith's apparatus uses rod-shaped coated electrodes for electric arc welding seams. A carrier member is provided and is guided in a predetermined course directed generally toward the work to be welded. An electrode holding member is connected to the carrier member in such a manner as to allow rotation of the electrode holding member and stop means are provided for opposing the rotation in one direction toward the work. When only a short length of the electrode remains, a further stop means restricts movement of the carrier member toward the work.

2,477,917—Gas Torch— Edward D. Wilson, Portland, Ore.

The gas torch covered by this patent has a passage for inflammable gas through the torch and a cooperating flint and abrasive element is provided for igniting the gas. Special control means are associated with the flint and abrasive element.

2,478,525—Welding Gun Adapter— John P. Cutrer, Houston, Tex., assignor to Reed Roller Bit Co., Houston, Tex.

The electrode rod holder disclosed by this patent has a tubular conduit assembly including an outer protective tubing and an inner rod holding tube extending through the tubing and terminating short of one end thereof. A tubular block and a tip element secured thereto are secured to the end of the outer tubing and an electrode rod may extend through the rod holding tube, block and tip element and may project therefrom.

2,478,636—Arc Welding Apparatus— Everett F. Potter, Schenectady, N. Y assignor to General Electric Co.

The arc welding apparatus disclosed in Potter's patent includes a plurality of electrode holders, having means for supplying arc-welding current to each of the

holders and to the electrodes mounted therein. The feature of the machine resides in mechanism for bringing one electrode holder into operative association with the work and then bringing a second electrode holder into engagement with the work immediately prior to the complete consumption of the electrode carried by the electrode holder then in operative association with the work.

2,478,640—Method of Welding by Induction Heating—Wesley M. Roberds, Collingswood, N. J., assignor to Radio Corp. of America.

The welding method in this instance is for joining of opposed spaced edges of electrically conductive work pieces by means of radio-frequency currents induced in the workpieces by induction heating applicator means. The induction heating applicator used is positioned between the spaced edges to be welded together and radio frequency currents are applied to the applicator to heat it by currents flowing therein and the adjacent edges of the workpieces by currents induced therein. Thereafter the edges are pressed against the applicator to unite therewith and form a permanent connection of the individual workpieces and the applicator.

2,478,944—Brazing Flux Composition—Albert E. Rising, Jr., Glen Head, N. Y.

This patented brazing flux composition includes potassium and borate compositions.

2,478,985—Apparatus for Multiarc Welding—Colby Weston Steward, Kenmore, and Malcolm R. Rivenburgh, Williamsville, N. Y., assignors, by mesne assignments, to Cornell Aero. Lab. Inc., Buffalo, N. Y.

This arc-welding apparatus includes a pair of carbon electrodes connected to an alternating current source to provide a heating arc in proximity to a workpiece. A direct current source is connected to a welding electrode that is movable adjacent to the alternating current heating arc to carry out the welding action. The alternating current arc is movable relative to a workpiece to vary the heating action produced.

2,479,087—Multiarc Electric Welding Circuit—Colby Weston Steward, Newton, Iowa, assignor by mesne assignments, to Cornell Aeronautical Laboratory, Inc., a corp. of N. Y.

The circuit disclosed in this patent is provided particularly for use with the apparatus covered in patent No. 2,478,985 and provides circuit control means so that a more balanced relation between the multiple arcs may be established.

2,479,092—Electric Arc Stud Welding Apparatus—Arthur Ratcliffe Ainsworth, London, England, assignor to Cyc-Arc Limited, London, England, a British company.

Objects such as studs, rivets, tubes etc. are welded to the surfaces of plates, bodies and like masses by this type of apparatus. The apparatus includes a body which is adapted to be positioned over the surface to which an object is to welded and controllable piston means are supported in the body and carry a chuck for the object to be welded to another member.

2,479,127—Electrode Holder for Arc Welding—George P. Loucks, Toledo, Ohio.

This patent discloses the use of a plurality of special type of link arms in pivotal relationship for positioning an electrode.

2,479,197—Submerged Melt Welding Fixture—Rea V. Anderson, Los Angeles, Calif., assignor to Rheem Mfg. Co., Richmond, Calif.

A specialized welding fixture is shown in this patent and it relates to the welding of an end closure member to a cylindrical shell assembly.

2,479,370—Rotary Welding Apparatus—Alonzo F. Kenyon, Wilkinsburg, and Theo. Specht, Sharon, Pa., assignors to Westinghouse Electric Corp., East Pittsburgh, Pa.

The primary novelty of this patent appears to reside in a specialized type of a transformer provided for use in rotary welding apparatus.

2,479,547—Adjustable Tip for Resistance Welding Electrodes—Clarence F. Wersler and Jos. F. Bukowski, Phila, Pa., assignors to The Budd Company, Philadelphia, Pa.

This patent is directed to an electrode for resistance welding apparatus and includes a tubular holder and a tip having a workpiece engaging surface, and a coolant coupling. Additional coupling means are provided for establishing a universal joint between the holder and the tip.

2,479,556—Welding Method and Apparatus—Harry Chanowitz, Chicago, Ill., assignor to David T. Siegel, Wheaton, Ill.

This relates to a specialized welding method for welding a filament or similar elongated workpiece to a cooperating work part. In the welding method a predetermined tension is established in the filament and the filament is welded to the workpiece while maintained under the predetermined tension.

2,479,798—Welding Fluxes—Rene D Wasserman, New York, N. Y.

Wasserman's welding flux comprises a large part of sodium carbonate monohydrate, a minor proportion of a hydrated alkali metal pentaborate and a small amount of graphite.

SECTION ACTIVITIES

Prepared by C. M. O'Leary

Pascagoula

A business meeting for the installation of new officers was held on July 7th at the Pascagoula Country Club. Dinner speaker was L. G. Weber who spoke on "History and Its Relation to Current Economics." W. D. Pelan, Chairman of the Section, spoke on the plans for the 1949–50 season.

Officers elected for the year are as follows: *Chairman*, W. D. Phelan, Ingalls Shipbuilding Corp.; *1st Vice-Chairman*, C. L. Cornell, Jr.; *Secretary-Treasurer*, I. W. Whittemore; *Chairman—Membership Committee*, John Howkins; *Chairman—Program Committee*, L. B. Weber; *Members at Large*—L. G. Weber, J. A. Furr and Hunt Teel.

Pittsburgh

The Pittsburgh Section, under direction of the newly elected officers, has completed its program for the coming 1949–50 season. Headed by the Chairman of the Membership Committee, F. H. Dill of the American Bridge Company, and ably assisted by Julius Heuschkel, Westinghouse Electric Corp., and Dr. H. L. Anthony of the Mellon Institute of Industrial Research, the following program has been arranged:

Oct. 26, 1949: "Safety in Arc Welding," W. W. Reddie, Westinghouse Electric Corp.

Nov. 16, 1949: "The Effect of Various Flux and Electrode Compositions on Submerged Arc Welding," Norman G. Schreiner, Manager of Unionmelt Service and Development, The Linde Air Products Co.

Jan. 18, 1950: "Steel Under Stress," Dr. M. Gensamer, Asst. to Director of Research, Carnegie Illinois Steel Corp.

FEB. 15, 1950: "Hard Surfacing," Howard S. Avery, Research Metallurgist, American Brake Shoe Co.

MAR. 15, 1950: "Inspection of Welding," W. B. Bunn, Welding Engineer, The M. W. Kellogg Co.

TRI-STATE—APR. 28, 1950: "What the Future Holds for Welding," T. B. Jefferson, Editor, *The Welding Engineer;* "Welding in Business," John B. Tinnon, Vice-President in Charge of Sales, Metal & Thermit Corp.

A cordial invitation is extended by the Pittsburgh Section to members of the AMERICAN WELDING SOCIETY, planning to be in Pittsburgh or its vicinity on any of the above nights, to visit the Section.

The officers for the 1949–50 Season are: *Chairman,* T. W. Morgan, Armco Steel Corp.; *Vice-Chairman,* E. H. Turnock, Westinghouse Electric Corp.; *Secretary,* J. F. Minnotte, Minnotte Brothers Co.; *Chairman—Membership Committee:* Richard L. Deily, Areway Equipment Co.; *Chairman—Reception Committee:* J. A. Merryman, Westinghouse Electric Corp.

Saginaw Valley

As of Sept. 1, 1949, Saginaw Valley will be known as a Section of the AMERICAN WELDING SOCIETY. Up to this time it was a Division of the Detroit Section, whose approval they have for becoming a Section.

Wichita

An open discussion on "Stump the Experts" was held at the May 16th meeting at Droll's English Grill, Wichita, Kan. The list of Experts included Lee Ormsbee, Harry Moberly, Earl Krasser, Delbert Hughes, R. R. Kirkland, R. E. Scott, Harvey Bates and John Wiley.

Announcement of new officers was made as follows: *Chairman,* H. W. Beeman, Tweco Products Co.; *1st Vice-Chairman,* Paul D. Carter; *2nd Vice-Chairman,* Harry L. Moberly, The Coleman Co., Inc.; *Secretary,* G. M. Allen, Kampton Welding Supply Co.; *Treasurer,* J. L. Ormsbee; *Chairman—Membership Committee:* George E. Shiner; *Chairman—Program Committee:* Paul D. Carter.

Employment Service Bulletin

Position Vacant

V-239. Wanted. Welding Engineer. Graduate Engineer with practical welding experience. Must be familiar with all phases of Welding, particularly Resistance Welding and Inert Gas Welding. Work will involve supervision and manufacturing development. Excellent job in an interesting field. State qualifications fully.

LIST OF NEW MEMBERS

August 1 to August 31, 1949

ATLANTA
Farrell, F. E. (B)

CHICAGO
Eilering, J. E. (C)
Rush, James J. (B)

CLEVELAND
Glatz, R. C. (C)

DALLAS
Glasgow, Lawrence G. (C)

DETROIT
Biel, Frank J. (B)
Farr, Robert H. (B)
Kemp, Alfred E. (C)
Lefever, Lloyd (B)
Sanford, Gilbert H. (B)
Stephenson, Harry (C)

KANSAS CITY
Brown, Andrew A. (C)
Olson, Ralph A. (C)

LEHIGH VALLEY
Stemler, Martin L. (C)

LOUISVILLE
Franklin, J. W. (C)

MILWAUKEE
Hirner, Frank J. (C)

NASHVILLE
Bringle, Bobby G. (D)

NEW JERSEY
Strand, Edward M. (C)

NEW ORLEANS
Holmes, Dick (B)

NEW YORK
Fischer, Maximilian J. (B)

NIAGARA FRONTIER
McKinsey, Charles R. (C)
Rodgers, F. Leo, Jr. (C)
Tanzberger, E. W. (C)
Wilson, Jack L. (C)

NORTHERN NEW YORK
Bonneau, Walter R. (D)
Cronk, Donald F. (D)
Duncan, Gordon G. (C)
Flynn, Howard P. (D)
Hovey, Howard G. (C)
Kryzak, Thomas (D)
Lehner, Joseph G. (D)
Luzinas, Edward C. (D)
Mallett, John H., Jr. (D)
Shoemaker, George D. (D)
Williams, Camille J. (D)
Zauko, Charles (B)

NORTHWEST
Trowbridge, Stanley C. (C)

PASCAGOULA
Jeudevine, G. H. (B)

PORTLAND
Caluwaert, John P. (B)

PUGET SOUND
Connell, Hal (B)

ROCHESTER
Wolak, Stanislow (C)

SAN FRANCISCO
Snyder, Arthur I. (B)

SYRACUSE
Mackey, Robert W., Jr. (C)

NOT IN SECTIONS
Biesta, D. T. H. (C)
Cheek, Lewis F. (D)
Kerr, James G. (C)
Mc Carthy, Neil (C)
McMurray, MacKenzie (C)

Members Reclassified

During month of August

CHATTANOOGA
McLain, A. R. (C to B)

CLEVELAND
Gilbert, Lewis (C to B)

DALLAS
Beutel, Herbert W. (C to B)

YORK-CENTRAL PA.
Logue, F. A. (C to B)

WELDING RESEARCH COUNCIL

of the Engineering Foundation

Sponsored by the American Welding Society and American Institute of Electrical Engineers American Institute of Mining and Metallurgical Engineers, American Society of Civil Engineers American Society of Mechanical Engineers, and Society of Naval Architects and Marine Engineers

Supplement to The Welding Journal, October, 1949

The Statistical Part in Welding Investigations

Using the science of statistics and special planning of investigation, it is possible to gain increased precision in estimates, enhance scope of conclusions, and savings in cost

by B. B. Day
(Illustrated by R. W. Miller)

Abstract

Variability is recognized as an inherent characteristic of all investigational work. Some pertinent sources of variation in welding processes are recounted from the literature, with a brief résumé of the extent to which the science of modern statistics has been used by welding investigators to handle variation. Chief emphasis was placed on repeated measurements with the arithmetic mean as the best estimate, standard deviation for measuring variability of these and randomization to eliminate bias. Also, use of the control chart technique for assuring acceptable quality in welding production has been urged.

Statistics as a science holds a unique place in experimentation. It provides a measurement of the uncertainty (always present) of conclusions drawn from experimental data through unbiased estimates of the variation. To accomplish this requires special planning of the test prior to the actual running of the test. It is only when the test has purposefully been so constructed that unbiased estimates are possible. One well-developed branch of modern statistics is devoted to the structural planning of experiments.

A hypothetical example is used to demonstrate the essential principles of experimental design and the efficient analytical procedure, known as the Analysis of Variance, for extracting the maximum amount of information inherent in an experiment so designed. The chief characteristics of the so-called "factorial" design not present in the classical one-at-a-time are: (1) inclusion within the same test of multiple factors at more than one level; and (2) the randomization of all possible sources of variation, including order of testing. These characteristics give the following advantages: (*a*) A valid basis for computing the numerical uncertainty of the inferences drawn from the experimental data; (*b*) gain in precision of estimates; (*c*) greatly enhanced scope of conclusions; and (*d*) a substantial saving in cost.

B. B. Day is Principal Statistician, U. S. Naval Engineering Experiment Station, Annapolis, Md.

R. W. Miller is connected with the Welding and Electrical Laboratory, U. S. Naval Engineering Experiment Station, Annapolis, Maryland.

Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

INTRODUCTORY SUMMARY

INDIVIDUAL measurements are recognized as unpredictable. Because of this element of variability, conclusions drawn from experimental observations have an element of uncertainty. Fortunately, repeated observations under uniform conditions do follow a surprisingly regular pattern. Through the structural design of the experiment, it is possible to adequately account for the lack of consistency and to obtain a measure of the uncertainty.

The factorially planned investigation is most effective for accomplishing these. It has two distinct characteristics: (1) a simultaneous study of several factors; and (2) all uncontrollable sources of variation are randomized. This type of structure is much more fruitful than the traditional method "one factor at a time holding all others constant." The same advantages would be evident over a haphazard test. Such an arrangement furnishes:

1. A valid measure of the inconsistencies in the observational data, as the basis for drawing inferences from observational data which the other methods of research do not furnish.
2. Maximum information on more points of interest than the classical one-at-a-time can possibly give—not only main effects of factors under study, but their joint effects at various levels, also the net contribution of each factor.
3. A broader basis for drawing conclusions, that is, for inductive reasoning.
4. Increased sensitiveness leading to the detection of small real differences which otherwise might not be evident.
5. A substantial saving in costs.

The Analysis of Variance technique, complementing the factorial experiment, evaluates the results and furnishes the quantitative measure of the uncertainty of the inferences drawn from them. It is also a method whereby the experimental error may be partially controlled and reduced.

It is urged that careful thought be given to the value in statistical planning of welding investigations before the actual experimentation gets under way. This would include both the structure and the skeleton of the plan of analysis. Such planning will lay the foundation for broader interpretation of the results, greater precision and economy in costs. In the words of R. A. Fisher, "The more thorough the design of an experiment, the more meaningful is the question asked."

INTRODUCTION

General Principles

Those concerned with investigation of welding problems will surely agree that research in this field follows the general pattern of all other experimentation. Thus the problem is stated; experimental work is carried on from which observational data are recorded; and, finally, conclusions or inferences are drawn on the basis of these experimental observations. In this connection, let us examine the purpose back of any investigation. Where does the chief interest lie? Is it only in the information about the particular specimens being tested, or is there an interest in the data from these specimens only to the extent that they will give a basis for conclusions about all of the same type or kind of specimens? For example, if a test were run to determine the minimum requirements for rod size in gas welding, the results of such a test would be of little interest if they did not furnish sufficient information for generalization on rod-size requirements generally, over and above the particular test. Hence, in any investigation we wish to be able to generalize for all of the same type, or kind, from the information gained in testing a few. In other words, we are learning about the group or family of objects having one or more common characteristics from observations made on a sample from that family.

If it were possible to measure the tensile strength of one gas weld made under certain conditions, and to say with certainty that that is the tensile strength of all such welds under the same conditions, the problems of investigational work would be pretty much solved. This, we know, is not the case. No one would be satisfied with a single observation, for, however much we attempt to control conditions in any experiment, there is always a random element present which causes the results to differ. We cannot invariably reproduce results however hard we try. This element of variability is a fundamental law of nature. It is one of the things of which we *can* be absolutely sure. The saying, "as alike as two peas in a pod," is a hoax; everyone knows that the peas in a pod are never exactly alike; and so with the individual items in the "pod," or set of measurements. Even modern physics is now recognized as dealing with aggregate statistical laws and quantities subject to errors and variation. Therefore, we must accustom ourselves to living with the idea of variation. Along this line Sir Charles Darwin in 1938 very aptly expressed the hope that generations would grow up which would have a facility to recognize that the inaccuracies and uncertainties of the world is one of its essential features.

Most investigators have an appreciation of this characteristic of variation, which plagues their experimental work at every step of the way and makes uneasy their conclusions. They are coming to realize that because of such inconsistencies conclusions drawn from experimental observations will always have an element of uncertainty.

If it were granted that investigational work could be carried on under perfectly controlled laboratory conditions and the research material was absolutely homogeneous, that is, devoid of any appreciable errors, the desirability of following such a procedure would still be doubtful. Conclusions reached from so artificial or rarified a test might be materially invalid under the ordinary practical conditions of application. Also the expense of attempting to set up such laboratory conditions would be prohibitive. At the other extreme would be investigations carried on under conditions of actual service or manufacturing practice. There would certainly be a place for this type of investigation. In between these two extremes are those under reasonably controlled conditions but with unavoidable variation recognized. A choice between service conditions and the last-named would depend on the scope and purpose of the investigation. In either case it is essential that the degree of uncertainty be measured quantitatively.

Defining the Purpose of the Paper

Specifically the statistical part in welding investigations is to provide a yardstick of the unavoidable variation in the observational data and to furnish methods of applying this measure to evaluate the results of an

experiment. It was for this purpose that modern statistical methods were developed.[8, 9, 17] Founded some 20 yr. ago by R. A. Fisher,[8, 9] and developed to a high degree by him and his associates, these methods have been used in recent years by engineers and physical scientists and found applicable to the solution of their problems (Aston,[1] Barnett,[2] Brownlee,[3] Poritsky,[13] Seder,[15] and Swan[16]). Using these methods, the maximum amount of information can be derived from an investigation, with the most efficient use of time, money, and personnel. By preliminary statistical planning, it is possible under certain conditions to greatly reduce the amount of testing which would otherwise be done. At the same time, these methods furnish a means of assessing the reliability and value of the information, so extracted, by estimating accurately the variation inherent in the experiment, called the experimental error. One is able to compute a numerical measure of the degree of confidence in the conclusions drawn from the test.

Incidentally, the reader will recognize that this idea of statistics, as a study of variation, is far removed from the concept of earlier days, when, as R. A. Fisher[8] states: "...for, until comparatively recent times, the vast majority of workers in this field appear to have had no other aim than to ascertain aggregate, or average, values." No attempt was made then to study variation. It was "recognized rather as a troublesome circumstance," detracting from the values of the average but about which nothing was done. We definitely are not concerned with such a notion of statistics; instead, we definitely are concerned with pertinent methods for dealing with variation.

You who are experienced in making investigations will realize that most of the methods presented here for handling variation have long been accepted as nothing more than ordinary common sense. What the statisticians have done is to systematize this common sense into a pattern equally applicable to investigational work in all scientific fields. One distinct advantage to be gained from following this pattern is that it provides the inexperienced investigator with an objective basis for arriving at the same conclusion which the experienced man might have reached intuitively out of the wealth of his experience.

It was thought that this group would be interested in a presentation of the general outline of these principles of experimentation of which there are two distinct phases. The first deals with the structure of the experiment; the second, with the evaluation and interpretation of the experimental data of the experiment which result from the statistical plan used. The two are so intimately bound together than neither is complete without the other. I shall attempt the Herculean task of presenting a very general picture of both by means of a hypothetical example. If this is sufficiently well done, those concerned with investigational work will want to explore the field through the listed references and others. Fair warning should be given that this science can prove to be a fascinating "baggage"! Numerous examples can be cited of engineers in other fields who have become so enamored of her wiles that they have followed her forth to carve out entirely new careers.

VARIABILITY IN WELDING PROCESSES AND PROGRESS MADE IN APPLYING STATISTICAL METHODS

To orient us for the main theme of this paper, we will look at some of the recognized sources of variation in the welding field and the statistical techniques proposed for coping with variation.

Recognition of Variability in Welding Literature

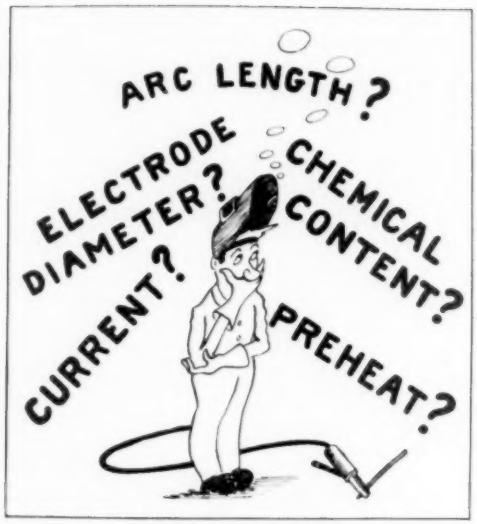
Clark[5] indicates that machines, processes and material are the chief sources of variation and that maximum weld quality would be achieved if these three were modified to yield perfect consistency. Then he spoils this happy arrangement by stating that neither science nor industry has discovered how to obtain such ideal consistency. More specifically, quoting from Hess and others[12] on spot welding in aluminum alloys: "The first requisites for consistency are reliable operation of the welding equipment with respect to secondary current and electrode pressure, and maintenance of uniform conditions at the electrode tips. The next most important factor affecting consistency is the magnitude of welding current. Uniformity of surface condition is another factor of major importance." Della-Vedova and Rockwell,[7] in a study of the effect of weld spacing on the strength of spot-welded joints, recognize that the established method of physical-testing tends to cause a twisting or bending of specimen, unequal stress distributions among the spots, particularly if testing jaws were out of line due to crooked clamping of specimens.

Leder[20] states that the factors affecting hardness, developed in a heat-affected zone, are composition of steel and cooling rate after welding. The latter in turn depends on size of electrode, welding current, speed of welding, size and shape of joint, etc. In the May 1949 issue of THE WELDING JOURNAL, C. J. Osborn and others on "The Composition and Property Variation of Two Steels" state: "...it is also known that within a single heat of steel, and even within a single ingot or plate, there occur marked differences in composition."

And so the case histories on sources of variation may be accumulated. Without exception, reports of all welding investigations which I have examined, recognize the presence of variation and its effect on the result.

Résumé of Statistical Techniques Used in Welding Field

Much progress has been made in using the tools of modern statistical methods to account for variation in welding investigations as outlined by Butler,[4]



Variation in welding

Random selection

Clark,[5] Della-Vedowa and Rockwell,[7] Hess and associates,[12] and Robinson.[14] First, they have followed the practice of testing more than one specimen and summarizing these by the mean (arithmetic average). Thus, the first principle of experimentation, the importance of taking more than one observation, has already been established. This need for multiple measurements, and even multiple tests, was appreciated as early as 1748 when Hume (London) stated in "An Inquiry Concerning Human Understanding," "...it being justly esteemed an unpardonable temerity to judge the whole course of nature from one single experiment, however accurate or certain." In addition to the average, these welding writers have emphasized the necessity for presenting a measure of extent of the differences among the multiple observations. They agree that the most efficient measure of this is the standard deviation. Robinson[14] has shown quite simply how the standard deviation may be used to compute maximum and minimum limits for the average strength of spot welds made under certain prescribed conditions for a certain risk factor, say for a 1-in-20 chance that the true average is outside the computed range. This is one of the valuable contributions of statistics to experimental work, namely to provide a method of computing the estimate of the precision of experimental results. Clark[5] effectively describes the principles of statistical quality control and its application in aircraft spot welding. He concludes that it is obvious that statistical control is essential for maintenance of high strength and consistency and as a means of measuring fairly the general quality.

One fundamental principle has been recognized and discussed, which should be emphasized as a basis for all that will follow. This is the principle of random selection for test work. Too much emphasis cannot be placed on the importance of randomization. While numerous examples could be pointed out in the literature on welding investigations where bias may have entered due to a systematic running of tests, all of one rod size, one chromium content before running second rod size, etc., the necessity for random arrangements in welding investigation has been emphasized by certain investigators. Butler[4] urges precaution in sampling from which inferences are to be drawn—choice of item without bias through random sampling. Clark[5] urges standardization of equipment and process within reasonable limits followed by random sampling since at least slight variations are bound to occur. Robinson[14] likewise states that specimens for checking must be randomly selected. The interested reader will find descriptions of other important statistical concepts and techniques in these welding references worthy of emphasis.

STATISTICAL STRUCTURE OF WELDING INVESTIGATION

Statement of Hypothetical Problem

Before launching into the discussion of the statistical planning of a welding investigation, may I beg your indulgence regarding the hypothetical example to be used. Please do not be too concerned if it falls far short of reality, either in the statement of conditions or in the data concocted from a fictitious experiment. It is only a means of finding a half-way meeting ground for our cooperative thinking. Under no conditions should the "conclusion" be taken seriously.

The problem: To study the effect of electrode size and chromium content on tensile strength of weld metal from different manufacturers.

The tensile strength of the individual welds might vary because of the effect of different levels of each of these three conditions; namely, electrode size, chromium content and manufacturer. These, we call assignable causes of variation. It is desired to segregate and measure the variation in tensile strength caused by each of the three; also, joint effects of any two in combination or of all three combined. It is evident that not all of the variation will be accounted for by these. In other words, duplicates of the same size electrode and chromium content from the same manufacturer will not show identical tensile strengths. This additional variation will be due to a number of unassignable causes, such as machine, material, temperature conditions, humidity, etc. This residual, or so-called unassignable causes, gives a measure of the experimental error and must be evaluated for an appraisal of the results of the experiment. It is only when the difference between the effect of electrode size, or chromium or manufacturer is measurably greater than the experimental error that a valid conclusion may be stated that one size electrode or one chemical content gives a higher tensile strength than another.

The fundamental requirement for valid estimates of effect of the factors being studied and of the experimental error of test is that the observations be free from bias. The precision of unbiased estimates increases with the size of the sample. On the other hand, if a bias is introduced in the test, increasing the sample will not overcome it. To achieve unbiased estimates, the effects of all possible sources of uncontrolled variation must be randomized. This means that each and every specimen must have an equal chance of being selected at every stage of the test. The order of selection must be left to chance. While it may never be absolutely attained in practice, every effort should be made to approach the ideal. Its importance cannot be overemphasized.

Keeping foremost this fundamental requirement of no bias in our estimates and adopting the principles of randomization as the only means of avoiding bias, let us see how we would proceed with the investigation.

After the selection of the controllable factors here, namely, (*a*) electrode size, (*b*) chromium content and (*c*) manufacturer, the next step would be the adoption for the experiment of a convenient number of levels for each of these factors. The range should cover minimum and maximum of special interest with intermediate levels, depending on the factor under consideration. This, as in the case of the controlled factors, will be decided on the basis of welding experience. The conditions of the selected factors in the example are: See Figure 1.

Electrode sizes: $^1/_8$, $^3/_{16}$ and $^1/_4$ in.
Chromium: 0.3, 0.5 and 1.25%.
Manufacturers: A and B.
Automatic welding under uniform conditions.
Welding current used for each manufacturer is its optimum.
Duplicate tests required.

It is recognized that there are other factors which should be taken into account but it is important to keep the illustration as simple as possible. In the selection of the material for the test, precaution must be taken at every step to avoid bias. Wherever selections are to be made of seemingly like materials, a random method of selection would be used. New heats should be made for each time the test is repeated. So far, so good—the decisions have been made on the basis of technical welding experience and there has been no departure from the usual procedure in conducting an investigation except possibly the extreme caution in randomizing.

Planning the Test

We are now ready to proceed with the testing. Let us first take the classical one-at-a-time method of research and see what happens. By this scheme one factor alone is varied, with all other factors held constant; the particular measurement at any one level of the factor being repeated until an assured estimate has been reached, then passing on to the next condition and repeating the process, etc. Using a standard or "normal" chromium level—say 0.5%, all made by manufacturer A—the $^1/_8$-in. size electrode is tested, repeating until an assured estimate, a "good" average, is found; then this process is repeated for the $^3/_{16}$- and $^1/_4$-in. electrodes. The same procedure would then need to be followed for the chromium content and for each manufacturer for information thereon.

It is reasonable to assume that a gradual change or adjustment might have taken place in the test equipment over the period of testing. Welding literature states that this is often the case. Temperature or humidity conditions, or a number of other possible factors, may have changed. With the above systematic order of testing, there would be no measure of such a change. The differences in tensile strength between the means of the $^1/_8$-in. specimens and that for the subsequent sizes would be hopelessly entangled with any progressive change in test conditions and with no possible way of unscrambling the effect of the two causes. Hence, there would be no way of estimating the experimental error. In addition to the fact that the classical one-at-a-time method would not yield a valid estimate of the experimental error, it has other limitations which will be indicated later.

The question then looms: what shall be the alternative? A phase of the modern science of statistics, the statistical design of experiments, will give the answer. This so-called "factorial experimentation" has been developed to a high degree, both from the standpoint of the basic mathematical theory and the variety of actual designs to cover a wide range of possible conditions and modifications. This method of experimentation differs in two fundamentals from the classical one-at-a-time method. It includes within

the same run all possible controllable factors under study and requires randomization of all sources of error, including order of testing; hence, the term factorial. Thus, an accurate estimate of the magnitude of the experimental error is provided. By including in the same experiment as many as possible of the factors whose effects are to be determined (Reference 3, Chap. I), the maximum amount of information is available on each of the main factors, independently, and in conjunction with each other.

Let us now look at this so-called factorial design for our problem. The general principles enumerated in this simple example will be equally true for the more complex ones having more than three levels for one or more of the factors and more than three factors.

With three electrode sizes, three levels of chromium content and two manufacturers, there are 18 possible combinations, $3 \times 3 \times 2 = 18$. We are equally interested in all of the 18.

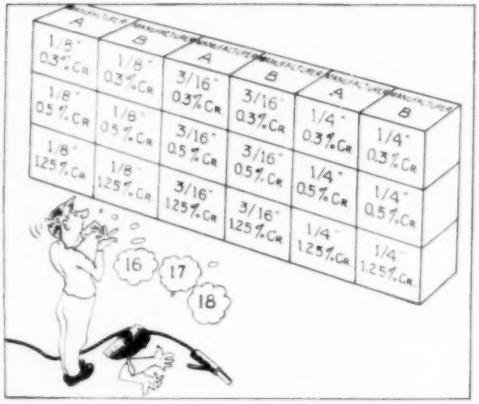


Fig. 1 "A Factorial, 3 × 3 × 2 = 18 Combinations"

Duplicate runs as a minimum will be required, making a total of 36 observations.

The first complete test of 18 will be finished before starting the second.

The electrodes in the second run should have come from different heats than were those in the first run. This precaution is to provide an accurate estimate of the true experimental error. The 18 combinations shall be tested in a random order with a different randomization for the second run from that followed in the first; this is to be done prior to starting the test. Drawing the numbers 1–18 from slips in a hat might be used. More conveniently, tables of random numbers are available (Reference 10, Table XXX).

The order of testing, as listed in Fig. 2, would be followed for the first run. Fictitious values for the resulting tensile strength for each of the 18 are tabulated in Table 1. Fictitious tensile strength values for the later second run are also given.

The statistical plan which has just been described is called the "Randomized Block" design. It is the simplest type. Each "block" is a complete replicate of

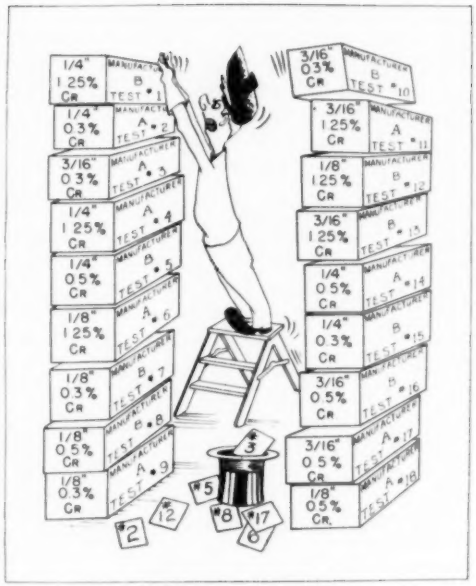


Fig. 2 Random order of testing

all the possible combinations of the factors being studied, tested in a random order. There are many statistical designs adaptable to more complex sets of conditions which not only provide for a valid estimate of error but increase materially the efficiency of investigations. References 3, 8, 11 and 17 are particularly valuable for a more complete understanding of this subject. Wernimont[18] gives additional source material.

The fundamental characteristics of "factorial" statistical designs are:

1. The simultaneous study of several factors.
2. Randomization of all sources of uncontrolled variability yielding a valid estimate of error.
3. Repeated observations supplying an estimate

Table 1—Combinations in Order of Testing with Observational Data for Duplicate Tests (Fictitious Data)

$3 \times 3 \times 2$ Combinations
(Chromium-Electrode Size-Manufacturer)

Order of Testing	First run: Combination tested (Cr-E-M)		First run: Tensile strength, psi.	Second run: Combination tested (Cr-E-M)		Second run: Tensile strength, psi.
1	1 25—1/4	B	77,800	1 25—3/16	A	80,600
2	0 3 —1/4	A	76,700	0 3 —1/4	A	76,500
3	0 3 —3/16	A	75,000	0 3 —3/16	B	74,400
4	1 25—1/4	A	80,600	0 5 —1/4	B	76,400
5	0 5 —1/4	B	74,800	1 25—1/4	A	81,600
6	1 25—1/4	A	80,700	0 5 —3/16	A	78,200
7	0 3 —1/4	B	75,100	0 5 —3/16	B	77,100
8	0 5 —1/4	B	76,000	0 3 —1/4	B	73,000
9	0 3 —1/4	A	76,200	0 3 —3/16	A	75,700
10	0 3 —3/16	B	74,000	0 3 —1/4	A	77,600
11	1 25—3/16	A	80,400	0 3 —1/4	B	75,800
12	1 25—1/4	B	78,800	0 5 —1/4	B	74,800
13	1 25—3/16	B	79,800	1 25—1/4	B	78,100
14	0 5 —1/4	A	78,300	0 5 —1/4	A	79,000
15	0 3 —1/4	B	72,200	1 25—3/16	B	79,200
16	0 5 —3/16	B	76,200	0 5 —1/4	A	78,800
17	0 5 —3/16	A	77,800	1 25—1/4	A	81,800
18	0 5 —1/4	A	79,200	1 25—1/4	B	79,400

of error and increasing the precision.

4. Balanced replicates of the combinations of factors.
5. Repeated observations, that is, absolute replications may be dispensed with where a large number of combinations is being tested and some several-factor joint effects are known to be insignificant. These will furnish estimate of the experimental error.

Evaluating the Efficiency of the Factorial Plan

Statistically designed investigations, as just outlined, would still be the most efficient to use if there were no better means of evaluating the data than the usual intuitive one of examining the mean values, drawing graphs, etc. Fortunately, the development of analytical methods for handling the data have kept pace with the growth of the efficient methods of planning the test. In fact, enunciation of the fundamental, general principles of this methodology for extracting the maximum information from experimental data by R. A. Fisher[9] superseded the planning by several years and was the basis for the design theory.

Before going on to the description of the statistical technique for evaluating the data of a factorial experiment, stock should be taken of the amount of information available from this plan.

Our first interest will be in finding out how reproducible were the observations. Since the possibility of bias was eliminated by randomization, the difference in the duplicates will give the figures for computing this so-called experimental error. These differences are listed in Table 2. The preponderance of differences with negative signs appears to indicate that some unknown factor had introduced variation between the two runs. Since all combinations were in each run and were tested in a random order in each, this difference will be balanced out in studying the effect of changing the levels of the factors. The actual computation for the experimental error appears in the appendix.

Table 2—Observational Data for Duplicate Tests by Chromium Content (Fictitious Data)

Combinations (Cr-E-M)		*Tensile strength, psi. (data coded* $\times 10^{-3}$*)* 1st run (1)	2nd run (2)	Sum (3)	Difference (4)
0.3 —1/8	A	76.2	77.6	153.8	−1.4
0.3 —3/16	A	75.0	75.7	150.7	−0.7
0.3 —1/4	A	76.7	76.5	153.2	0.2
0.3 —1/8	B	75.1	75.8	150.9	−0.7
0.3 —3/16	B	74.0	74.4	148.4	−0.4
0.3 —1/4	B	72.2	73.0	145.2	−0.8
			12	902.2	
			Mean	75.2	
0.5 —1/8	A	79.2	78.8	158.0	0.4
0.5 —3/16	A	77.8	78.2	156.0	−0.4
0.5 —1/4	A	78.3	79.0	157.3	−0.7
0.5 —1/8	B	76.0	76.4	152.4	−0.4
0.5 —3/16	B	76.2	77.1	153.3	−0.9
0.5 —1/4	B	74.8	74.8	149.6	0.0
			12	926.6	
			Mean	77.2	
1.25—1/8	A	80.7	81.8	162.5	−1.1
1.25—3/16	A	80.4	80.6	161.0	−0.2
1.25—1/4	A	80.6	81.6	162.2	−1.0
1.25—1/8	B	78.8	79.4	158.2	−0.6
1.25—3/16	B	79.8	79.2	159.0	0.6
1.25—1/4	B	77.8	78.1	155.9	−0.3
			12	958.8	
			Mean	79.9	
			Grand total	2787.6	
			Grand mean	77.4	

To consider chromium content, we reshuffle the values in Table 1 into the three chromium groups as listed in Table 2. Of the 18 readings in each run (replicate), there are six at each of the three levels. Combining the duplicate readings gives 12 for each of these. In all other ways the combinations of electrode and manufacturer in the three sets are balanced. Therefore, comparisons of the totals of three sets will give a valid measure of the effect of altering the chromium level. Our three average values (coded) are 75.2, 77.2 and 79.9 psi. Note that we are basing the comparisons on 12 independent determinations for each. As stated by Robinson[14] and others, the precision of an average increases with the number of determinations on which it is based.

Those of you with a background of welding experience would intuitively be able to state conclusions with considerable confidence at this point. We will defer attempting this until the next section.

Similarly Table 1 could be arranged into two parts by manufacturer. The first three in each of the chromium groups are by A; the remaining three, by B. Thus, there would be nine electrodes in each, and with duplicates, a total of 18 each. These two sets are balanced by chromium content and electrode size. The difference between averages 78.6 and 76.3 psi. indicate the manufacturing variation.

In like manner the readings could be grouped by electrode sizes, three groups of six each in duplicate with all chromium levels and manufacturers balanced. This time the averages, 78.0, 77.4 and 77.0 psi., will give us a valid estimate of the effect of altering electrode size from 1/8 to 3/16 to 1/4 in. Again, each of our averages are based on 12 observations and have the same precision as the chromium averages. Thus, with only 36 determinations, we have measured the effect of three levels each of chromium and electrode size with a precision based on 12 observations, and of two manufacturers with a precision based on 18 observations.

It is important to note that each of these main comparisons pertain not to any special set of the other two factors but are over-all effects for the ranges within which the two factors were taken in this experiment. Thus, comparisons of electrode size cover a range of 0.3–1.25% chromium content for manufacturers A and B; likewise for chromium and manufacturer. The scope of our conclusions or inferences is much enhanced by this fact. It is evident that efficient use has been made so far of the 36 observations; but this is not all, for we have by no means exhausted the amount of information which is inherent in the 36 readings. Not only will the interest lie in the over-all effect of changes

in the levels of each factor, but also in the interdependence of the influence of two or more of the factors, aptly called "interaction." Will an effect of one factor, that is, a change in chromium on tensile strength, be the same for both small and large electrodes, or might the effect of chromiun be enhanced for small electrodes? In other words, are the effects of the levels of the different factors additive?

The family physician has a special word for such a built-up effect. He calls it synergism and gives as an illustration: morphine in $^1/_8$-grain doses has a certain positive effect on the patient; in $^1/_4$-grain doses, a greater effect. Likewise, hyoscine, another drug, will tend to have the same sort of an effect as morphine when given in $^1/_{120}$-grain doses; but giving the patient a combined dose of $^1/_8$-grain morphine and $^1/_{120}$-grain hyoscine will have a profound effect far greater than would be the sum of the effects of the drugs if taken separately. On the other hand, certain levels of two factors may have a counteracting effect on each other so that the net result is less than either of the two taken separately.

Our data will provide quantitative measures of the intereactions between the factors of the example.

Below is the table of average values which are the basis for studying the interaction of electrode size by manufacturer. Note that each average in this table is based on six observations.

Manufacturer	*Electrode size, in.* (*coded* × 10^{-2} *psi.*)			
	$^1/_8$	$^5/_{16}$	$^1/_4$	*All*
A	79.0	78.0	78.8	78.6
B	76.9	76.8	75.1	76.3
Both	78.0	77.4	77.0	77.4

For the A electrodes, the changes are 1.0 and −0.8 psi. for increase in electrode size; for the B electrodes, the corresponding changes are 0.1 and 1.7. Are these two sets of changes significantly different?

A similar table is possible for the joint effects of manufacturer and chromium; likewise, the latter with electrode size, with averages based on four observations each.

Thus, you have seen something of the scope of the information available from the 36 observations in a factorially planned experiment. Let us see how that compares with what would have been available had the classical one-at-a-time method been followed, limiting the testing to 36 specimens (hence the same cost involved):

(*a*) *Chromiun.* If the effect of varying chromium were studied by the classical method, the other factors would have been held constant at one of the levels chosen as a standard. The same degree of precision as in the factorial would have required 12 determinations for each of the three chromium levels, a total of 36 specimens tested. This is the number in the complete test above. Also the difference in tensile strength for chromium would have most likely been entangled with a bias because all of one level were tested consecutively before testing the next chromium level, etc. The scope of the conclusions regarding chromium would have been limited to one electrode size only and a single manufacturer; while by the factorial the scope was enlarged to a range of electrode sizes and two manufacturers.

(*b*) *Electrode Size and Manufacturer.* The sample of 36 for varying chromium would have given precise estimates of one electrode size and manufacturer, but no information on the other sizes and source with which to compare these.

(*c*) *Interactions or Joint Effects.* No information whatever on the interdependence of the three levels; that is, the joint effects could not be derived from the 36 specimens as tested by the classical method. This limitation places a serious handicap on effective experimentation.

(*d*) And last, but by no means least, there would have been no valid estimate of the true experimental error of the test. No basis for judging the uncertainty of the significance of any apparent differences.

ANALYSIS OF A STATISTICALLY PLANNED EXPERIMENT

For the most part others who have written on this subject have concluded their papers at this point. However, to do so leaves out a very important phase necessary to complete the picture, that is the statistical analysis of the data. In the preceding section it was indicated that the seasoned investigator would arrive at his conclusions intuitively from a combination of past experience and an examination of the observational data. There is, however, a statistical tool, a very powerful one, called the Analysis of Variance technique, for doing this objectively. Given this tool, even the young inexperienced man will not only be able to extract the maximum amount of information inherent in the observational data of a factorially planned experiment but he will have the quantitative measure of the uncertainty of the conclusions which the data have justified. This, the intuitive method does not furnish.

The Analysis of Variance states that the total sum of the squares of the deviations of the individual observations from the mean (average) of all in a properly designed test, can be partitioned into the various sums assignable to the factors under control (called main effects), their joint effects and a remainder measuring the effect of uncontrolled factors. In our example there are several possible causes of variation in the readings: three main effects—electrode size, chromium content and manufacturer; three two-factor joint effects—electrode size with chromium and with manufacturer, and chromium with manufacturer; one three-factor effect—the three combined; and finally, the variation between duplicate readings. The complete Analysis of Variance of our example would be much too complex for presentation. In has been done, showing the arithmetic involved, but is relegated to an Appendix. It

would also be inappropriate to attempt a complete and detailed description of this method. The textbooks listed in references 6, 9, 11, 17 and others will furnish this.

For demonstration purpose we will use the simplest possible case, which would be one factor only under study. Using the fictitious data, we will assume (1) that only the chromium content was allowed to vary at the three levels indicated, electrode size and source had varied in a random manner; (2) such runs of three levels of chromium were repeated 12 times each, each time in random order, yielding the 36 observations. Then the total variation in the 36 observations would be due to two causes: (1) the three chromium levels; and (2) random or uncontrolled factors for the 12 units within each level (Table 2). Thus, the total variation is equal to the variation between levels of chromium plus the variation among the individual items within each level. As stated above, variation is measured in terms of the sum of squares of deviations of the individual observations from the mean of all. If x stands for the individual observation in a sample of N such observations, and $\bar{x}$ for the mean of the N items, then $\sum^{N}(\bar{x}-x)^2$ would be the total sum of squares where $\sum$ is a summation sign.

This may be expressed algebraically for our example as follows:

$$\sum^{36}(x - \bar{x})^2 = 12\sum^{3}(\bar{x}_{Cr} - \bar{x})^2 + \sum^{3}\sum^{12}(x - \bar{x}_{Cr})^2$$

Where x represents the individual observation, $\bar{x}$ is the mean of the 36 observations; $\bar{x}_{Cr}$ is the mean of a level of chromium, of which there are three; and the Greek letter sigma, $\sum$, is the summation sign.

Thus, for the 36 observations $\bar{x} = 77.4$. Then the deviations of each of the 36 individual readings from this mean, squared and summed, would be the total sum of squares mentioned above. For the coded data, this value is 206.56. It is this value which can be broken down into sum of squares due to the two sources of variation. A conventional, standard form has been devised for writing these down. Our total sum of squares is recorded at the foot of this table in the column headed "Sum of squares." You will note that in the left-hand stub are listed the sources of variation as named above.

Analysis of Variance (Data Coded)

Source of variation	*Degrees of freedom* (1)	*Sum of squares* (2)	*Mean square* (3)	*F* (4)
Between chromium levels	2	134.33	67.16	30.68
Within chromium levels (error)	33	72.23	2.1889	$P < 0.001$
Total	35	206.56	Standard deviation	$= \sqrt{2.1889}$ $= 1.48$

Subtracting the general mean, 77.4, from each of the three means for levels of chromium in Table 2, squaring these differences, summing and multiplying by 12 as in the formula above, gives the sum of squares due to differences in levels of chromium. The "within-chromium-levels" sum of squares can be found directly by the formula above where each of the 12 readings within a level are subtracted from their mean, squared and summed, and the three such totals combined. If the arithmetic is correct, this will check with the difference between the total sum of squares in the foot of the table and that in the first row for chromium levels. This value, 72.2333, will give the measure of the error in the test.

There are two other columns, (1) and (3), which need explaining. "Degrees of freedom" is the number of possible independent comparisons between the items of a group or sample, and is always one less than the number of items. Thus for three levels of chromium, there are two possible independent comparisons. We can compare the mean of the highest level with that of the lowest and these two with the mean of the middle level. The first would measure the linear relationship; the second, the curvilinear. It would be possible to break down the 134.33 into the part contributed by each of these two degrees of freedom. Usually this is of real interest to the investigator. For the "Within-chromium levels" there will be eleven independent comparisons for each of the three levels. The degrees of freedom in the analysis, likewise, add up to that for the total, $2 + 33 = 35$. Column (3), headed "Mean square," is an average value, the quotient of columns (2) and (1). For chromium it is 67.16; for within-chromium, 2.1889. The mean square is the core of the Analysis of Variance technique. Its magnitude, as you see, depends on the magnitude of the differences from which it is computed; its precision, when estimating the experimental error, depends on the corresponding number of degrees of freedom.

We can now complete the evaluation of the data so that "conclusions" may be drawn to which can be affixed quantitative measures of their uncertainties. The reasoning is as follows: if a factor does not have a significant influence, its mean square would be just another estimate of the experimental or error mean square and would be substantially equal to it, varying therefrom only by random errors. On the other hand, if the factor does have a real influence, its computed mean square will be noticeably larger than the error mean square since it will be due to both sources of error, the factor and random.

We test this by finding the ratio of the mean square under scrutiny to the mean square of the experimental error: that is, 67.16 to 2.1889. Probabilities for such F-ratios have been computed, and tabled (Reference 10, Table V) for judging whether or not the ratio is sufficiently large for changes in that particular factor to have had a significant effect. For our example, $F = 67.16/2.1889 = 30.68$. Referring to this standard table,[10] we find for a ratio of 30.68 the probability, P, is less than 0.001. In other words the chance is very small, less than 1 in 1000, that a ratio as large as 30.68 would have been due to random errors alone. The

conclusion drawn is that the amount of chromium content has a real effect on tensile strength with the quantitative measure of the uncertainty of this conclusion less than 0.001. So small a chance of being wrong places a high degree of confidence in the conclusions.

The relative importance of chromium as a source of variation in tensile strength can now be evaluated. If, from the mean square 67.16 we remove the part due to random causes, the error variance (mean square), the remaining part will measure the true chromium effect called "variance." Reference 3, Chapter 10, has an excellent treatment of this.

$$\text{The true chromium variance is} = \frac{67.16 - 2.19}{12} = 5.41$$

Variances have the desirable characteristic of being additive (standard deviations are not). Therefore we can express the relative importance of the chromium in this way, using V for variance:

$$\frac{V_{Cr} \times 100}{V_{Cr} + V \text{ error}} = \frac{5.41 \times 100}{5.41 + 2.19} = 71\%$$

Thus, it is estimated that 71% of the variation in welding under these conditions will be due to differences in levels of chromium. This process applies equally well where there are several factors as shown in the appendix. This type of information is highly valuable in the planning of subsequent investigations; in evaluating the importance of factors causing variation in factory production; and in setting specification limits.

And finally the square root of the error variance, $\sqrt{2.1889} = 1.48$, is the experimental error, the best estimate of the standard deviation for measuring the precision of the individual mean values. Using this, it would be possible to determine which of the three average values differed significantly.

SUMMARY

Thus we have two entirely different approaches for carrying on research: the traditional one-at-a-time holding all else constant, and the factorial which has been described herein. The former classical approach is searching along a narrow path with a meticulous avoidance of side issues. Under carefully controlled test-tube conditions this narrow path is continued until an answer is reached, hopefully deemed satisfactory, with fingers crossed.

Instead of this piecemeal way of attacking a problem, as if causes and effects formed a daisy chain to be counted off one at a time, the factorial makes a vigorous attack on all the possible causes at the same time under reasonably normal conditions. They are all thrown into the same hopper. To the uninitiated, it might appear that this gives a garbled mixture impossible of unscrambling. However, back of this seeming confusion there will have been preliminary and purposeful mixing or planning, completely balanced in details, with complete directions, the Analysis of Variance technique for the unscrambling. Such a plan will yield measures not only to the effects of the individual causes but their joint effects as well. An added advantage is the fact that under this way of doing research, things can be studied under normal working or factory conditions. There is not the risk involved in having to translate test-tube results to reality.

"Classical one- at-a-time Method of Testing"

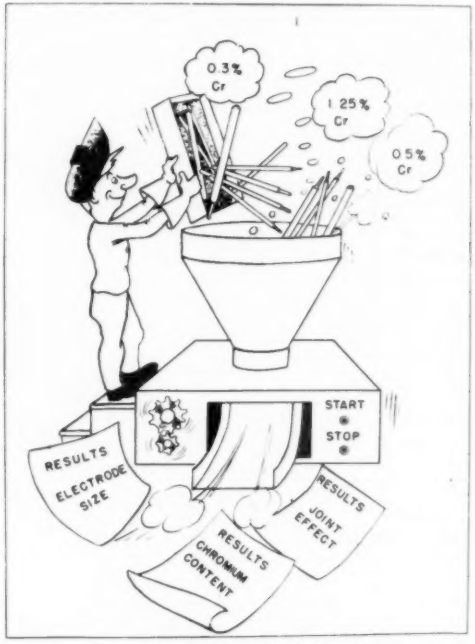


Fig. 6 Factorial method of testing

ACKNOWLEDGMENTS

Acknowledgements are made to: (1) Bela Ronay,

Supt., Welding and Electrical Laboratory, U. S. Naval Engineering Experiment Station, Annapolis, Md., at whose suggestion this paper was prepared; (2) staff members of the Welding and Electrical Laboratory, U. S. Naval Engineering Experiment Station, Annapolis Md.—in particular to O. E. Swenson and N. L. Navarre for welding technical assistance in planning the hypothetical example and for constructive reviews of the paper; (3) F. R. Del Priore, Associate Statistician, U. S. Naval Engineering Experiment Station, Annapolis, Md., for his excellent and untiring help throughout the preparation of this paper, both in constructive suggestions and in the complete preparation of the fictitious data; and (4) John G. Rutherford, Asst. Chemical Inspector, Glenn L. Martin Co., Baltimore, Md., for constructive review of the paper.

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Glossary

1. *Assignable Causes of Variation*—Those factors under study whose variability is controlled.
2. *Degrees of Freedom*—The number of possible independent comparisons that can be made in a set of observations.
3. *Experimental Error*—An estimate of variation caused by the uncontrolled factors in an experiment.
4. *Factorial Design*—One in which all the possible compositions or combinations of the factors being investigated are included in one test.
5. *Interaction*—The mutual effect of two or more factors on one another's action. Their joint effects are not the sum of effects of the several factors taken singly.
6. *Main Effects*—The direct effect of any factor or agent without reference to the effect of other factors.
7. *Mean*—The arithmetic average of a set of observations.
8. *A random sample* is one drawn in such a manner that every item has an equal chance of being selected. A random sample is unbiased—selected "blind."
9. *Randomized Block Design*—The simplest type of factorial experiment in which all combinations of factors under control appear in a single test.
10. *Standard Deviation*—The root mean square of the deviations of the individual measurements from their mean; hence, the square root of the variance.
11. *Random Error*—The variation due to factors not controlled in the experiment.
12. *Variance* is the mean value of the squares of the deviations of the observations from their mean.

Appendix

Example to Illustrate the Analysis of Variance of Data from a Factorially Designed Experiment

(A Hypothetical 3 × 3 × 2 Electric Welding Problem)

Conditions of Experiment

Three levels of chromium: 0.3, 0.5, and 1.25%.
Three electrode sizes: $^1/_8$, $^3/_{16}$ and $^1/_4$ in.
Two manufacturers: A and B.
Duplicate runs, that is, two replications.
$3 \times 3 \times 2 = 18$ combinations.
$2 \times 18 = 36$, total number of observations.
Original data: Tensile strength coded.

Steps in Computation

1. The total sum of squares is found by squaring each result and summing. The sum must be adjusted for the grand mean, called the correction factor, C. $C = n(\bar{x})^2 = 36(77.43333)^2 = 215853.16$. The total sum of squares is then: $(75.0)^2 + (74.0)^2 + (75.7)^2 + \ldots + (74.8)^2 - C = 206.56$

2. The main effect "chromium content" is found by squaring the mean of each chromium content. The sum of the three squares is multiplied by the number of items which went into a mean. The sum of squares must be adjusted for the grand mean. Thus, using Table 2: $12[(75.2)^2 + (77.2)^2 + (79.9)^2] - C = 134.3267$, the sum of squares due to chromium. In the actual calculations the means were not rounded as shown here.

In a similar manner the sum of squares due to the manufacturer is: $18[(78.6)^2 + (76.3)^2] - C = 48.5344$.

Also the sum of squares due to electrode: $12[(78.0)^2 + (77.4)^2 + (77.0)^2] - C = 6.4867$.

3. To obtain a two-factor-interaction sum of squares, the two-factor table of mean values is needed, similar to the Table A, for the Cr × E interaction term.

Table A—Mean Tensile Strengths for Computing Joint Effects of Chromium Content and Electrode Size

Chromium content, %	*Electrode size, in.*		
	1/8	3/16	1/4
0.3	76.2	74.8	74.6
0.5	77.6	77.3	76.7
1.25	80.2	80.0	79.5

The mean values are squared and the sum multiplied by the number of items going into a mean. From this sum is subtracted C and the sums of squares of the two factors, Cr and E.

$$4[(76.2)^2 + (74.8)^2 + \ldots + (79.5)^2] - C - 134.3267 - 6.4867 = 1.9817$$

The two-factor table, 2 × 3, of manufacturer and chromium yields the M × Cr interaction sum of squares. Similarly, a 3 × 3 table yields the M × E interaction term. The three-factor interaction term is obtained by using the three-factor table.

4. The error term or the sum of squares due to the duplicates is obtained by taking the differences between the first and second run for each condition as in Table 2. These differences are squared and summed. The sum is divided by 2, thus:

$$1/2[(-1.4)^2 + (-0.7)^2 + \ldots + (-0.3)^2] = 4.3100.$$

DISCUSSION

Thus the sum of squares for each of the listed sources in Table B was computed independently and recorded in Column 2. It is an extremely gratifying fact that the sum of these will always equal the total sum of squares as computed in Step 1 above. Likewise, summing the number of independent comparisons, the degrees of freedom add up to the total possible comparisons which is one less than the sample size of 36. The degrees of freedom for joint effects are the product of those for the corresponding main effects. Being able to partition the total variation into the contribution of the several components, makes the Analysis of Variance technique the most powerful tool of modern statistics.

The mean square will in each case be the sum of squares divided by its respective degrees of freedom. These mean squares are the basis for drawing inferences, for they are the quantitative measures of the effects on tensile strength of varying the levels of each of the controlled factors and of their effect in combination. The duplicates furnish an estimate of the variance of the test. Reading from Table B, this is 0.2394.

As explained in the body of this paper, the variance ratio is the basis for evaluating the significance of a particular effect. This phase of the analysis will only be briefly touched upon here. Starting at the bottom of Table B, the three-factor-interaction mean square,

Table B—Analysis of Variance of a 3 × 3 × 2 Factorial Experiment for Investigation of Tensile Strength in Electric Arc Welding (Fictitious Data)

Source of variation	*Degrees of freedom (1)*	*Sum of squares (2)*	*Mean square (3)*
Main effects			
Chromium content, Cr	2	134.3267	67.163
Electrode size, E	2	6.4867	3.243
Manufacturer, M	1	48.5344	48.534
Two-factor interactions			
Cr × E (2 × 2)	4	1.9817*	0.4954
Cr × M (2 × 1)	2	0.5489*	0.2744
E × M (2 × 1)	2	9.5355	4.7678
Three-factor interactions			
Cr × M × E (2 × 2 × 1)	4	0.8361*	0.2090
Duplicates (exp. error)	18	4.3100*	0.2394
Grand total	35	206.5600	

* Pooled for new estimate of error variance, 0.2742.

0.2090, is slightly less than the experimental mean square (variance), 0.2394. Hence, there is no evidence of a significant change in tensile strength due to the combination of the three factors—electrode size, chromium and manufacturer. Of the two-factor interactions, the only mean square sufficiently large to yield a significant ratio is 4.7678 for the electrode-manufacturer interaction. The computed chance is less than 1 in 1000 that so large a ratio would have been due to sampling alone. Hence, we would "conclude" on the basis of this experiment that the combination of the sizes of electrodes from the two manufacturers did not show the same effect on tensile strength; the effects were not additive. An examination of the means previously given indicates that for A electrodes there is little change in tensile strength for different sizes; for those furnished by B, there was a definite change with size. It would be appropriate at this point to pool the sum of squares of the experimental error and all the interactions that were not significant for a revised estimate of error mean square (variance). Pooled, these would be Cr × M, E × C, E × Cr, E × Cr × M, and the experimental error yielding a variance of 0.2742 with 28 degrees of freedom. The increase of 28 over the 18 gives a definite improvement in precision.

Next we will consider the main effects. The chromium mean square, 67.163, is many times larger than the estimated error variance, 0.2742, which includes all the interactions of chromium with electrode and manufacturer. Hence, there is a real effect of chromium on tensile strength, for the chance is very small, less than 1 in 1000 that the difference in tensile strength for chromium might have been due to random variation.

Let us examine the electrode mean square, 3.243. It is larger than the error variance but not as large as the interaction of manufacturer and electrode interaction, 4.768. This means that differences in tensile strength due to electrode size are not consistent for the two manufacturers, and so a generalization would not be valid. The ratio of the mean square for manufacturer 48.53, to either error variance or the M × E interaction

4.768, is sufficiently large to draw the inference that there is a real difference in tensile strength between sources of electrodes, all sizes, and chromium levels, with a very small chance, less than 1 in 1000, of being wrong. Electrodes made by A have the greatest tensile strength.

Because the chromium mean square is larger than that for manufacturer, 67.16 against 48.53, does not necessarily mean that chromium effect is the more important. We must disentangle from these the error variance present. The resultant values will be the estimates of the true or "pure" effect of each. The individual variances can be computed from the observational data of an adequately planned experiment. (See Reference 3, Chap. X, for the theory on this.) As stated previously, variances have the very desirable characteristic of being additive. Thus, if the variability in some quantity—say tensile strength—is due to a number of independent sources, the total variance of tensile strength would be the sum of variances arising from these different sources. The total standard deviation of the test would then be the square root of this sum of the variances. The resulting, estimated true variances for our example are:

	Estimate of true variance	%
Chromium	11.1482	63.2
Manufacturer	4.9476	28.1
Manufacturer × electrode (and E)	1.2438	7.1
Error	0.2742	1.6
Total	17.6138	100.0

Thus, chromium has by far the most important effect on tensile strength, accounting for 63% of the total variance; manufacturer, a substantial effect, 28%; interaction of electrode size and manufacturer, 7%; and the error variance, less than 2%. This information can be extremely useful as pointed out in the text.

The possibilities of information from such a planned investigation have been indicated in a very general way only. There is much more specific information which could be gained from this initial Analysis of Variance breakdown. For example, one might be interested in a further breakdown of the degrees of freedom for chromium, which was significant, to determine whether the effect was linear only or curvilinear.

Heat Effects in Anode Spots of High-Current Arcs

A method is proposed for evaluating the rate of energy input to the anode surface. Correlation of melting rates with standard heat-flow equations may be possible

by T. B. Jones, W. B. Kouwenhoven and Merrill Skolnik

Abstract

Anode spots are formed in a d.-c. arc system when one electrode in the form of a flat steel tape is moved at high speed relative to the other electrode in the form of a rod. The number of spots formed per second and the areas of the individual spots are determined over wide ranges of tape speed and arc current. The surface melting rates thus obtained give a picture of the energy distribution in the anode spots and its effectiveness in melting the steel tape. Highest melting efficiencies are shown to exist at the higher tape speeds. Apparent current densities in excess of 9000 amp. per square centimeter are indicated at the anode. Correlation of melting rates with standard heat-flow equations may be possible at higher speeds.

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Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

INTRODUCTION

THE School of Electrical Engineering of The Johns Hopkins University has been carrying on for some time a research program dealing with the fundamental characteristics of high-current arcs. This program is under the sponsorship of the Office of Naval Research and has as its objective the development of new information which will lead to a better understanding of arc phenomena. It is an inherent part of this program that the results of the work be made known to all who may be interested in arc behavior so that by

free discussion and further experimentation the frontiers in this field may be broadened.

A previous paper[1] has described a portion of this work which would seem to be of particular interest to those who use the arc for welding and cutting purposes. It was shown that a stable d.-c. arc could be maintained between a fixed electrode in the form of a rod and a moving electrode in the form of a flat steel tape even when the speed between the two electrodes was many times in excess of normal welding speeds. With the electrode at negative polarity and the tape positive, the track left by the arc on the moving tape was not continuous but consisted of a line of melted spots of uniform size and spacing. These were called anode spots since they seemed to represent the source of greatest activity of the arc as it transferred from one location to another in moving along the anode surface. The possibility of using the anode-spot phenomena as a new approach to obtaining a better understanding of the basic-arc mechanism has been explored,[2-4] and it is the purpose of this paper to give a brief presentation of the progress of experimental work carried on in this connection during the past year. In analyzing the results of this work it has been possible to draw conclusions of only limited extent. It is hoped, however, that a presentation of the experimental results as they are obtained may give rise to discussion and suggestions from those working in this field so that the course of further experimental work may be charted in the direction most likely to lead to a solution of some of the arc problems.

APPROACH TO THE PROBLEM

The distribution of the arc energy at the anode surface determines in a large measure the effectiveness of the arc in melting the work material. This distribution of energy is determined by the area over which the current enters the anode surface, i.e., the current density. If the current enters the anode over a sufficiently broad area, little or no melting of the surface would occur since the heat would be carried away at a too rapid rate by radiation, conduction, etc. Conversely, if the current density is sufficiently high, the metal is brought to the melting point quickly and the melting proceeds at a rapid rate often producing excessive spattering and an unstable arc.

The line of anode spots produced by the high-speed electrode system seems to offer a method for obtaining some picture of the order of magnitude of the current densities involved and, at the same time, the heat effects produced thereby. The method is to determine the number of spots formed per second and the area of the individual spots over a range of speeds and currents. The increase in area of the spots with time gives a measure of the lateral melting rate produced by the arc on the anode surface which is a function of the energy distribution at the surface. Also, dividing the total arc current by the area of the individual spots gives a picture of the probable order of magnitude of current densities involved. The data obtained in this manner when plotted in curve form for various speeds and currents appear to offer possibilities for a quantitative evaluation of some of the heat effects of the arc and the controllable factors which govern them.

EXPERIMENTAL METHOD

All of the tests were conducted with straight polarity, i.e., the electrode negative (cathode) and tape positive (anode). A $^1/_4$-in. diameter carbon rod was used as the cathode and 0.028-in. thick by 1-in. wide mild steel tape formed the anode. The separation of the electrode from the tape was $^1/_8$ in. and a welding generator set was used as the source of power. A complete description of the test apparatus has been covered in previous papers.[1, 2]

The speed of the tape was varied from about 35 to 400 ft. per minute. For each value of speed in this range, runs were made at eight values of current, ranging from 50 to 200 amp. Following each series of tests, the tape was removed and measurements were made on the line of anode spots left by the moving arc. The number of spots per revolution of the tape could be counted and knowing the revolutions per minute of the lathe spindle, the number of spots per second could be determined. The area of individual spots was measured using a calibrated microscope.

MELTING RATE IN ANODE SPOTS

A single anode spot is shown in Fig. 1. The area melted is clearly defined and can be accurately measured with a microscope. Figure 2 shows the variation of the area of the individual anode spots with the total arc current. The results indicate a linear increase with current for each electrode speed. At a constant current the individual areas become smaller as the speed is increased. The linear relationship with current indicates that over the speed and current ranges covered the heat which is causing the steel surface to melt is not an I^2R type heating but rather an EI

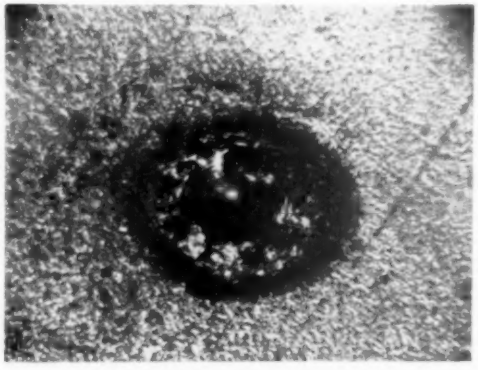

Fig. 1 Anode spot magnified 30X (actual diameter 1 mm.)

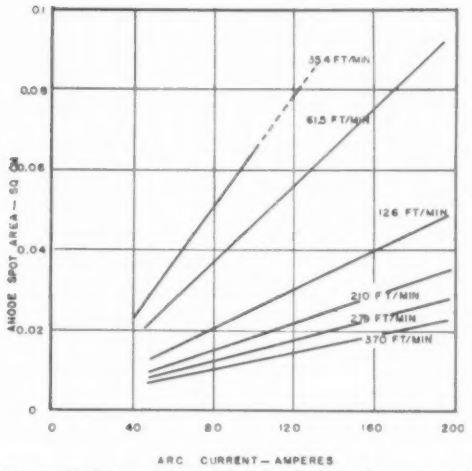

Fig. 2 Variation of area of individual anode spots with total arc current

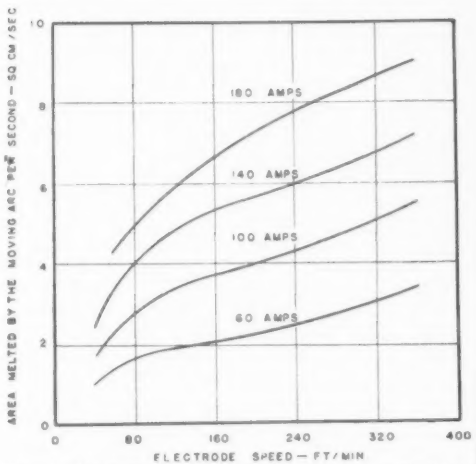

Fig. 4 Variation of the total lateral area melted per second with speed of the moving tape

type heating. This means that the energy necessary to cause the melting is produced by charged particles forming the current, falling through the potential drop at the anode surface. If the anode drop remains constant, then the power input is a linear function of the current.

Taking data from the curves of Fig. 2 and combining this data with the number of anode spots per second as shown in Fig. 3, the curves of Fig. 4 are obtained. These show the rate of melting of the anode surface as a function of the electrode speed. It is seen that the lateral rate of melting increases in an approximately linear manner as the electrode speed is increased. This indicates that the heat which produces melting of the anode surface is most efficiently utilized at high rates of relative electrode speeds.

The variation of the area of individual anode spots with electrode speed is shown in Fig. 5. The curves appear to be of the form of equilateral hyperbolas tending to become asymptotic to some value of area at electrode speeds greatly in excess of those reported here. Using the data from these curves and from Fig. 2 it is possible to obtain a relationship between the area of the individual anode spots and the time in milliseconds that the arc is burning in each spot. These curves are shown in Fig. 6 at 60, 100, 140 and 180 amp. The intercepts of these curves with the horizontal time axis should give a measure of the time required to begin melting the steel surface in an anode spot at each occasion when the arc forms at a new location on the tape. Such data may then be correlated with standard heat-flow

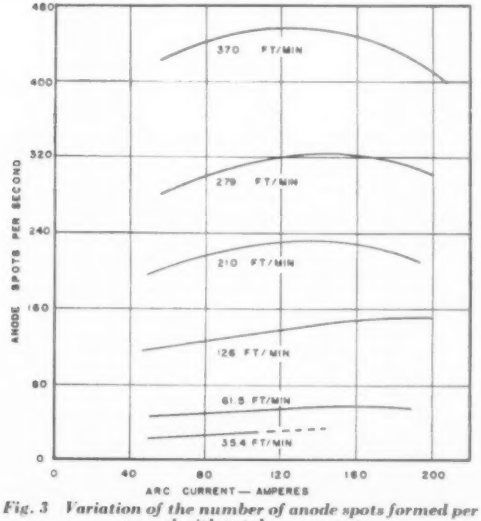

Fig. 3 Variation of the number of anode spots formed per second with total arc current

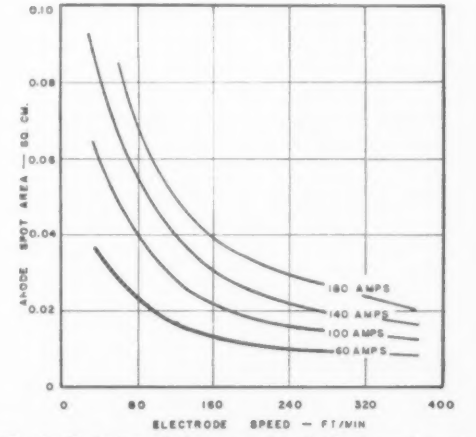

Fig. 5 Variation of the area of individual anode spots with speed of the moving tape

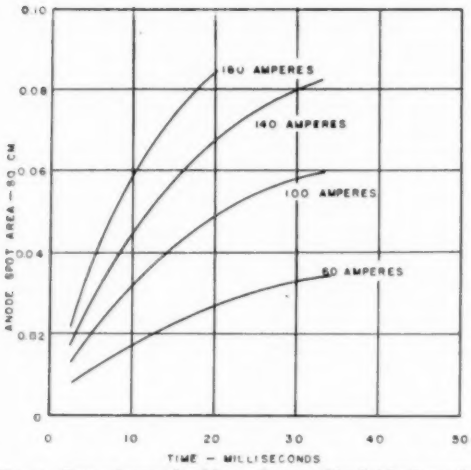

Fig. 6 Rate of growth of lateral area of individual anode spots

equations as indicated below. To obtain such intercepts, however, tests at considerably higher speeds must be carried out. Design of apparatus to make these tests is now in progress.

ANALYSIS OF RESULTS USING HEAT-FLOW EQUATIONS

An attempt was made to correlate the results described above concerning the rate of growth of melted area in an anode spot with standard heat-flow equations. This necessitates an assumption regarding the manner in which the current enters the electrode. The first assumption made was that the current entered at a point and all of the heat was produced at this point. Taking the steel tape surface as a semi-infinite solid at zero initial temperature, the equation for this condition is as follows:[4]

$$Q = \frac{4\pi krt}{erfc\ (r/\sqrt{4\alpha\theta})}$$

where

k = specific heat, cal./gm ° C. = 0.087.
α = thermal diffusivity, cm.^{2}/sec. = 0.071.
t = melting point of steel = 1500° C.
r = radius of melted area, cm.
θ = time required to melt surface to radius r.
Q = heat input, calories/sec.
$erfc$ = error function.

Values of k and α were taken at 600° C., this being considered as the average temperature from a cold surface to the melting point at 1500° C.

Substituting the experimental values from Fig. 6 in this equation, produced values of calories per second in excess of 15,000 as being the heat input to the anode surface necessary to produce spots of the observed area. This is better than twenty times the total heat output of the entire arc and indicates clearly that the current does not enter at a point but rather over a finite area. It also indicates that the volume radius of the melted metal is less than the surface radius.

Assuming that the current enters over an area A, the time required to produce melting at the center of the area is given by the following equation:[5]

$$\theta = \frac{330000\ A^2}{p^2}$$

where

θ = time in milliseconds.
A = area in sq. mm. over which current enters.
p = power input, watts.

This appears to offer the greatest possibility for correlation of experimental results with theory since the intercepts of the curve of Fig. 6 with the horizontal axis are a measure of the times for initial melting. Knowing these times, the power input per unit area at the anode could be computed giving a measure of the energy distribution in the arc. Calculations based on estimated times show good correlation with the known energy of the arc. As mentioned above, to obtain these intercepts will necessitate tests at considerably higher speed.

CURRENT DENSITY TESTS

The current density at the electrode surface is one of the most important arc parameters and one of the most difficult to evaluate. The definite outlines of the melted area in the anode spots, as shown in Fig. 1, offer an approximation to the area over which the current is entering the anode spot. This, of course, may not be true area since the current may enter in a smaller or larger area than indicated by the melted anode spot. However, it should indicate the order of magnitude of current densities involved to a closer degree than can be obtained using a stationary electrode system.

Referring to the values thus obtained as "apparent current density," the results at three arc currents are

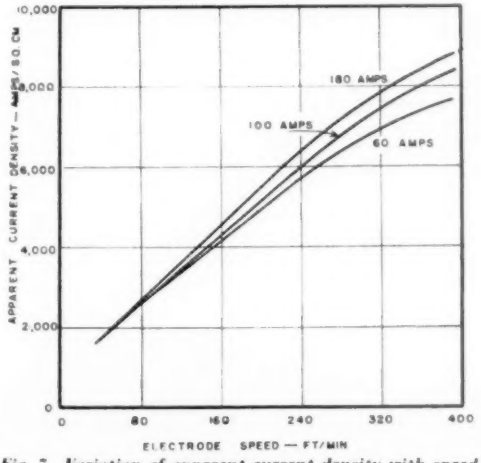

Fig. 7 Variation of apparent current density with speed of the moving tape

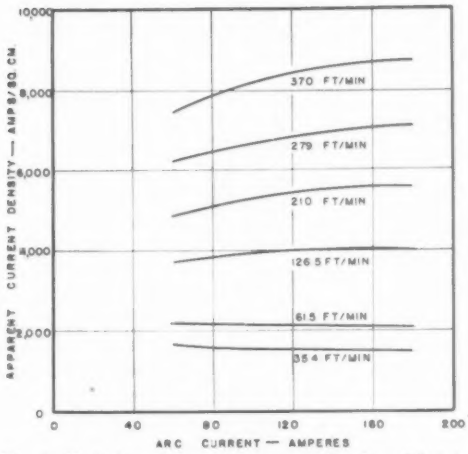

Fig. 8 Variation of apparent current density with total arc current

shown in Fig. 7. It is seen that the apparent current density increases in an approximately linear relationship with the electrode speed showing some tendency to level off at speeds in excess of 400 ft. per minute. This indicates a final asymtotic value of current density in excess of 9000 amp. per square centimeter. Tests at higher speeds are planned in order to obtain a better evaluation of the final value.

The variation of apparent current density with current at various electrode speeds is shown in Fig. 8. These curves indicate that the apparent current density is approximately constant at a given speed.

CONCLUSIONS

Systematic measurements on the line of anode spots formed in the track left by a d.-c. arc on a moving electrode have indicated the following:

1. The rate of growth of the area of individual spots offers possibilities for evaluating the rate of energy input to the anode surface and its distribution on the surface.
2. The area of individual spots when multiplied by the number of spots per second gives the total surface areas melted and is a measure of the lateral melting efficiency of the arc. The results indicate higher efficiencies at the higher speeds.
3. Determinations of apparent current density show values in excess of 9000 amp. per square centimeter, tending to increase with electrode speeds.

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Atomic Scientist's Aid Finds New Job Counting Alloy Factors in Steel

THE Geiger counter, famed for its ability to detect uranium and other radioactive materials, and also as an aid to nuclear physicists in atom-smashing development, has found a new and extremely useful job in the steel industry.

It has been adapted by scientists of the Research Laboratory, United States Steel Corp., of Delaware, to the development of a quick and accurate method of analyzing steel samples. After careful calibration for many specific problems, many of which are yet to be solved, Geiger counter analysis is expected to be even faster than the direct-reading spectrograph, which is itself a recent development in metallurgical analysis. In the laboratory studies, it is used in conjunction with special spectrographic equipment and has achieved a measure of success in revealing quickly the kind and quantity of alloying elements in steel.

This does not mean that the chemical elements which are combined with iron to make steel are radioactive, as might be inferred from the Geiger counter's more celebrated job. As it is capable of detecting and measuring radioactivity, this remarkable instrument is capable also of measuring invisible fluorescent rays, each of a special kind, that emanate from a steel sample when its atoms are ionized by powerful X-ray.

The sample's iron atoms and the atoms of alloying elements are not smashed in the analysis process, as might be expected. In fact, the steel sample is in no way harmed or changed.

In many ways it is really simpler to make a Geiger counter analysis than an ordinary chemical or a spectrographic analysis of steel. Almost any piece of steel that has a flat surface will do for the purpose, so long as it is reasonably clean. Thus analysis might be made of a random section of steel rail, the bottom of a cooking pot, a knife blade or a flattened mass of steel file dust.

"It has long been known that chemical analysis is possible from fluorescent or secondary X-rays," explained Dr. James B. Austin, director of research and head of the United States Steel laboratory, Kearny, N. J. "In principle, a specimen may be bombarded with electrons or X-rays strong enough to ionize the various atoms; the process of electronic rearrangement of these atoms gives rise to X-rays which are characteristic of the several elements present in the specimen. Measurement of the quantities of the various rays emitted is a short-cut to chemical analysis.

"Work in this field has been handicapped by the lack of intense X-ray sources and the necessity in the past of measuring the intensities of the rays by the blackening of photographic film. Even with crude equipment, however, it has been possible to detect elements of middle-atomic weight in quantities as low as one-hundredth of 1 percent of the specimen being studied. To accomplish this it was necessary to mount the specimen directly on the target of an X-ray tube, to use film exposures over many hours and rigidly to control film processing and density measurement. With the advent of stronger X-ray tubes and the Geiger counter, this type of fluorescence analysis has become relatively simple and much more useful."

A 20-milliamp., 50-kv., molybdenum target X-ray tube is used in the United States Steel laboratory studies. The ray floods the steel specimen, exciting secondary rays which are characteristic of the chemical elements alloyed with iron in the

(Continued on page 483-s)

Electrical Resistance Offered to Nonuniform Current Flow

Contact resistance consists of resistance of actual areas in contact on the resistance set up by nonuniform flow of current though material. The results of this latter type of resistance is covered in this paper

by W. B. Kouwenhoven and W. T. Sackett, Jr.

INTRODUCTION

WHEN two metallic surfaces are brought into intimate contact with each other, the surfaces never make perfect contact over the entire area. Instead the actual contact occurs only at small subareas. This phenomenon is known as contact resistance. It is important in resistance welding and other applications such as electrical contacts and switches.

Contact resistance may be considered as consisting of two parts. They are the resistance offered to the current flow by the areas in actual contact, and the resistance set up by the nonuniform flow of current through the material. The first is called the contact resistance proper, and the second the spreading resistance. The contact resistance proper is affected by the surface finish[1] and the presence of absorbed gases, oxide films, grease films, etc., on the surface. The contact resistance[2] is also affected by the pressure applied to the surfaces, the temperature and the current density in the areas in actual contact.

This paper presents the results of a study of the phenomenon of spreading resistance and its contribution to the total resistance of contacts. The description of the experimental investigation is given, and then the results are discussed and compared with theory. The work was made possible by the Resistance Welding Fellowship established at the School of Engineering of The Johns Hopkins University by the Resistance Welding Committee of the Welding Research Council.

SPREADING RESISTANCE

When uniform current flow occurs in a specimen, the current density is the same throughout the specimen. The resistance R of the specimen may be written $R = \rho\ (l/A)$; where ρ is the specific resistance, l the length and A the cross-sectional area.

It is well known that current flow in a specimen with a constriction is not uniform. The current follows definite stream lines and the current density in the corners is reduced. Figure 1 shows a plot of equipotentials and flow lines determined experimentally on a brass strip with a sharp constriction. Note that the flow lines become substantially parallel within the dimensions of the strip width from the constriction The spreading resistance is due to the reduction of current density in the corners which contributes an increase in the resistance.

In a contact between two clean metal surfaces, where no film exists at the interface, the only resistance present at the contacts is the spreading resistance. The actual contact area will be much smaller than the apparent contact area because of surface irregularities; the spots where actual contact occurs will be randomly distributed over the apparent contact surface; and the current constricting effect of these spot contacts contributes the total contact resistance in this case. Incidentally, in this case, the contact members adhere as if welded at the spot contacts.[3] Such contacts can be realized only between metals which have been cleaned, completely degassed and maintained in a vacuum.

EXPERIMENTAL INVESTIGATION

In all electrical contacts the spreading resistance is of fundamental importance. It was decided to study the spreading resistance independent of the contact resistance proper. An experimental and theoretical investigation was undertaken with the following aims:

1. To determine the magnitude of the spreading resistance as a function of the size of the conducting spot or constriction.
2. To find for a given constriction the relationship between the spreading resistance and the resistivity of the material.

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Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

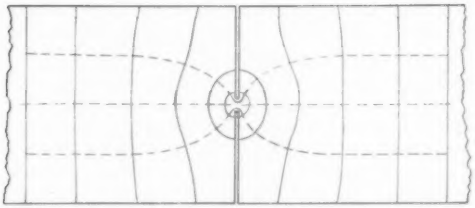

Fig. 1 Equipotentials and current flow lines on a metallic strip with a constriction

3. To determine the effect of the location and shape of the constriction on the spreading resistance.

Although an analytical approach to the first question is possible, the mathematicals involved are rather complex. In setting up the equations and summing the series encountered, it is necessary to make certain simplifying assumptions, among them that the spreading resistance for a given constriction is proportional to the specific resistance of the material. An exact analytical solution of the third question is not possible. An experimental verification was therefore desirable.

SPECIMENS

The method adopted for the study of the spreading resistance was to determine the increase in resistance produced by making lathe and saw cuts of different depths in the central section of round rod specimens. A specimen is shown in Fig. 2. Each specimen was first checked for uniformity of diameter and resistance. The uniform rod was then immersed in an oil bath and its resistance, between two fixed knife edges 8 in. apart, accurately measured in a Kelvin Bridge. The next step was to reduce the area of the central section of the rod for a length of $^1/_2$ in. When this was completed the resistance was again measured between the two knife edges.

If the resistance of the rod for uniform current flow per unit length of an area A be designated as R_A, the total resistance, R_t, for a specimen whose central section is of reduced cross section can be divided into components $R_{A1}l_1$, $R_{A2}{}'_2$ and R_s. Where $R_{A1}l_1$ is the resistance of the length l_1 of cross section A_1 and $R_{A2}l_2$ is the resistance of the length l_2 of cross section A_2.

These two resistances may be calculated on the basis of the known dimensions, and the specific resistance. Then from the measured value of R_t the spreading resistance, R_s, may be computed from the equation:

$$R_s = R_t - (R_{A1}l_1 + R_{A2}l_2)$$

The specimens were made mainly from round stock, of diameters of $^5/_8$, $^3/_4$ and 1 in. In addition a number of flat specimens were studied. The materials used included copper, aluminum, brass, bronze, iron, nickel, Everdur and copper nickel. This provided a range in specific resistance from that of copper with 1.78×10^{-6} ohm-cm. to copper nickel with 37.6×10^{-6} ohm-cm.

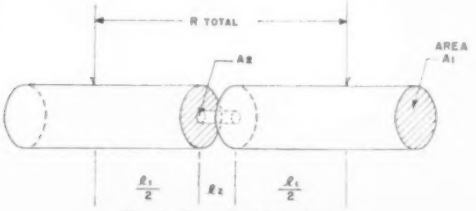

Fig. 2 Round rod specimen

APPARATUS AND TECHNIQUES

The method of measurement used was a difference method, and therefore it was important to obtain as high a degree of accuracy as possible. A Kelvin Bridge circuit was chosen for the work and the resistances were measured with direct current.

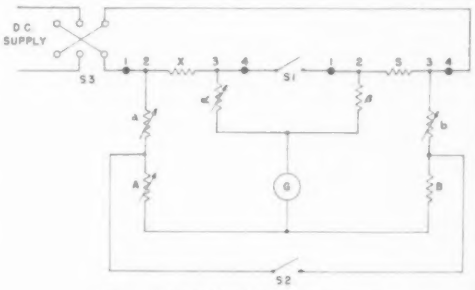

Fig. 3 Kelvin double bridge circuit

The Kelvin Double Bridge used was designed and built to handle specimens ranging from 18 micro-ohms (the resistance of an 8-in. length of $^5/_8$-in. diameter copper rod) to 500 micro-ohms for the higher resistance specimens. This bridge had a sensitivity of one part in 10,000.

The bridge circuit used is shown in Fig. 3. The unknown resistance is X and the standard, S; A and B and α and β are ratio arms. S_1 and S_2 are switches that are used in the manipulations of the bridge. The balance equation is given by the expression: $X = (A/B)\,S$.

In this bridge an uncertainty of 1° in temperature in either measurement would introduce an error of roughly four parts in 10,000. In order to minimize this error as much as possible it was decided to use as the standard resistances, S, uniform rods of the same diameter and material as the specimen. The resistance of each standard was chosen so as to make it possible to operate the bridge close to the correct ratio for maximum sensitivity.

Standards for each material tested were constructed and the resistance of each specimen both before and after cutting was measured in terms of a given standard

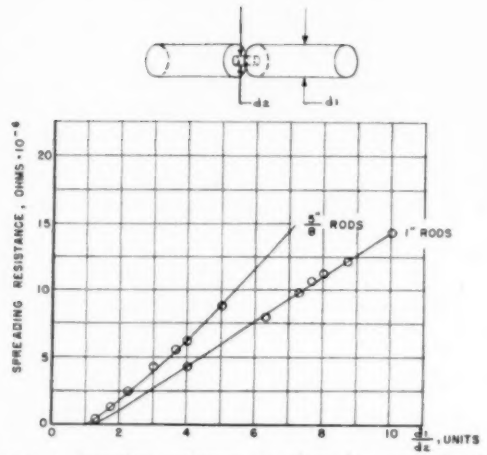

Fig. 4 Spreading resistance in ohms for two rods of different diameters having varying constrictions

of the same material. These standards had the same temperature coefficient as the specimens and were immersed in the same oil bath. The temperature problem was thus simplified.

Care was taken in the operation of the bridge to prevent local heating caused by too large a current through the unknown, X, and the standard, S. At all times the accuracy was better than one part in 1000. This was found to be ample as the spreading resistance proved to be higher than first expected.

RESULTS

In Fig. 4 are presented the results of a series of measurements made on $^5/_8$ and 1-in. diameter bronze rods having constrictions of different amounts. The abscissa is the ratio of the diameters of the full-sized rod to the diameter of the constricted portion and the ordinate spreading resistance in micro-ohms. Perhaps a more revealing plot is shown in Fig. 5 where the spreading resistance is plotted in terms of the equivalent length of the full diameter rod against the ratio of diameters. The resistance of this equivalent length is calculated on the basis of uniform current flow in the rod.

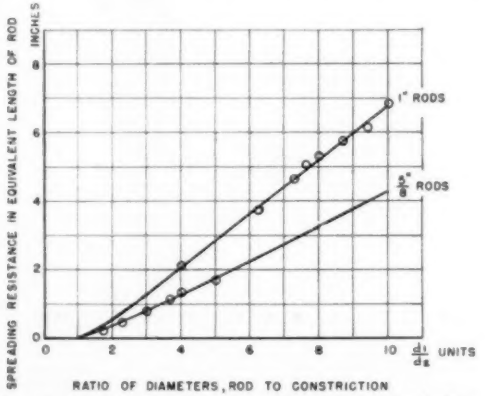

Fig. 5 Spreading resistance expressed as an equivalent length of rod for $^5/_8$ and 1 in. diameter rods with constrictions

This plot shows for example that, for a 1-in. diameter specimen with a constriction 0.1 in. in diameter at its center (ratio of diameters 10 to 1), the spreading resistance is equivalent to a rod length of 6.5 in. This is a surprisingly large amount when it is remembered that the lines of current flow become substantially parallel within a relatively short distance from the constriction, certainly not more than $1^1/_2$ in. in this case.

A method of converting spreading resistance to an equivalent length of rod which clarifies this apparent paradox is to express the spreading resistance in terms of the equivalent length of the constricted section. The spreading resistances involved indicate that the equivalent length of the constricted area is of the order of 0.03 in. This is a more nearly true picture of the actual location of the spreading resistance; the major portion of which lies close to the constriction. This is of importance in resistance welding for it shows that the heat developed by voltage drop in the spreading resistance is produced at and very near the contact surfaces.

Equivalent Cone

A study of the results plotted in Figs. 4 and 5 shows that for each diameter specimen there is a separate curve. In order to correlate the results on rods of different diameters a nondimensional quantity $R_s d_1/\rho$ was evaluated.

The derivation of this particular quantity is based on the simplifying assumption that the current flow in the material at the termination of the constriction could be considered as confined to the frustum of a right circular cone of an angle Φ as shown in Fig. 6. Here a

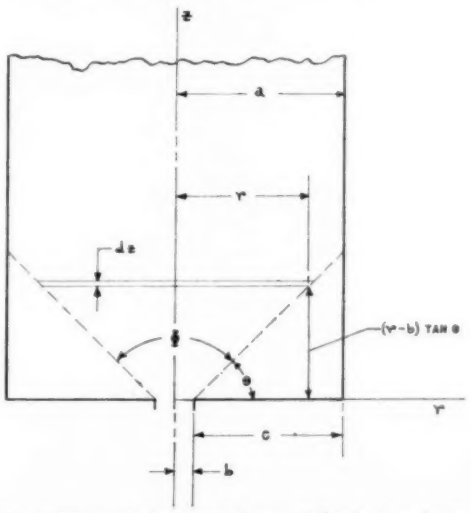

Fig. 6 The equivalent cone of current flow in round specimens

is the radius of the rod and b the radius of the constricted portion.

Let r be the radius of a circular slice or element of the cone taken perpendicular to the Z axis; then for an angle θ, where $\theta = 90 - \Phi/2$,

$$\frac{d}{dz}\,\text{Req} = \frac{\rho}{\pi r^2}$$

where Req equals the resistance of the element of radius r and

$$Z = (r - b)\tan\theta\left(\frac{1}{b} - \frac{1}{a}\right)$$

The resistance of the rod, based on physical boundaries, if no spreading effect were present, is

$$R = \rho\,\frac{\tan\theta}{\pi}\left(\frac{a - b}{a^2}\right)$$

The spreading effect, as defined above, is

$$\text{Req} - R = \frac{\rho}{\pi}\tan\theta\left(\frac{1}{b} + \frac{b}{a^2} - \frac{2}{a}\right)$$

So far only the upper half of the sample has been considered. The total effect will be twice this, or:

$$\frac{R_s \times d_1}{\rho} = \frac{4}{\pi}\tan\theta\left(\frac{a}{b} + \frac{b}{a} - 2\right)$$

This simplified calculation shows that $(R_s d_1)/\rho$ is a function of a/b, the ratio of the radii. If θ is chosen to be 41°, a rather good fit between this formula and the experimental data is obtained.

A similar calculation was carried out for a flat strip (two-dimensional constriction), assuming an equivalent wedge of flow (Fig. 7). For an arbitrary θ, $R_s w/\rho$ = $\tan\theta\left(ln\,\frac{a}{b} + \frac{b}{a} - 1\right)$. Here the value of θ which gave the best fit with experimental data was, coincidentally, 41.3° (Fig. 8).

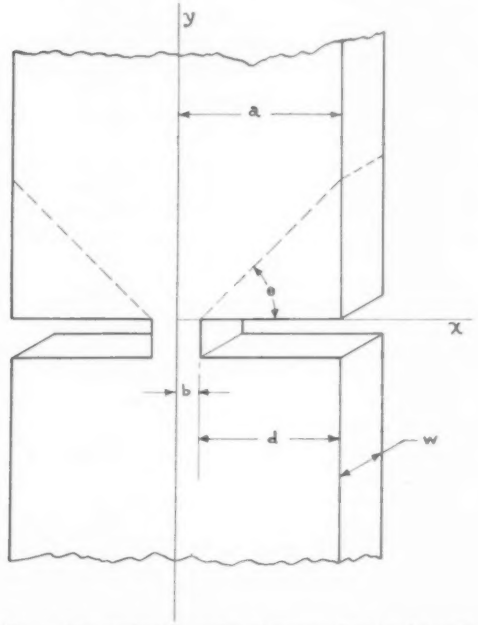

Fig. 7 The equivalent wedge of current flow for flat strip specimens

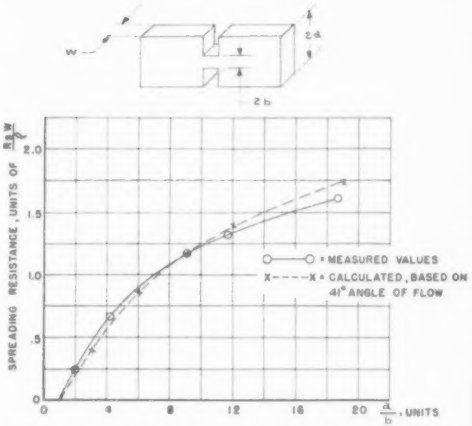

Fig. 8 Comparison of experimentally determined and calculated values (based on equivalent wedge of current flow) of spreading resistance for flat strips

Here it should be emphasized that this concept is of value only in visualizing the magnitude of the spreading effect; it does not give a true picture of what is happening at the constriction.

Analytical Solution

The exact solution of even the symmetrical problem is very complex, but it has been made by Dr. L. C. Roess[4] for the case of heat flow through a right circular cylinder with a symmetrical constriction. His solution, with the notation changed to agree with that of Fig. 6 and the electrical case is:

$$\frac{R_s d_1}{\rho} = \frac{a}{b}\left|1 - 1.4093\left(\frac{b}{a}\right) + 0.296\left(\frac{b}{a}\right)^3 + 0.0525\left(\frac{b}{a}\right)^5 + \ldots\right|$$

This solution has been plotted in Fig. 9 together with the curve for a 98° equivalent cone of flow.

The points plotted in Fig. 9 are for specimens of copper, aluminum, iron, nickel and alloys of these metals ranging in outside diameters from 0.45 to 1 in., and with varying size constrictions. The ordinate of Fig. 9 is the dimensionless quantity $(R_s d_1)/\rho$ and the abscissa, the ratio of diameters. The measured spreading resistances are shown by the individual points coded for the different materials. In addition there are plotted the curve for the Roess equation and the curve for the equivalent cone. A study of Fig. 9 shows that the results of the measurements when plotted to these coordinates lie on a smooth curve. This curve is independent of both the material and its dimensions. The results check the theoretical equation.

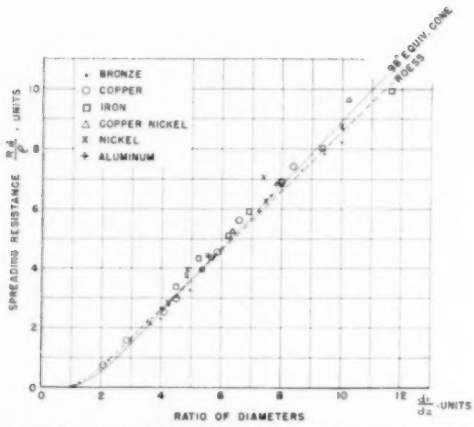

Fig. 9 Experimentally measured values of spreading resistance for round rods of different diameters and materials containing constrictions varying in amount and length and corresponding curves for the Roess and equivalent cone equations

Length of Constriction

In practical contacts the length of the constriction is often quite small. The earlier measurements were made on specimens with a constriction $^3/_8$ in. in length. To determine the effect, if any, of the length of the constriction on the spreading resistance, a number of specimens were made with constrictions varying from 0.163 to 0.007 in. in length. The spreading resistance of these specimens was measured and the results plotted in Fig. 9. The values found lie on the curve and check with those found for the longer constrictions. The spreading resistance is, therefore, independent of the length of the constricted portion.

Location of Constriction

In practice, actual contact areas are both irregular in shape and random in location. The effect on the spreading resistance of nonsymmetrical or eccentrically located constrictions was studied for both round and flat strip specimens. The constriction was held to a fixed size and on successive specimens its location was varied from the center to the extreme edge. The results are plotted in Fig. 10 and the curves for both round and flat specimens are similar in shape.

A study of Fig. 10 shows that the increase in spreading resistance for off-center cuts is small until the eccentricity exceeds 60%. The effect is naturally larger for the flat strip, for here the current can spread in only two directions and the flow lines are more severely restricted by the eccentric location. These results depend on the size of the contact area. For a very small contact area, the effect of eccentricity is less.

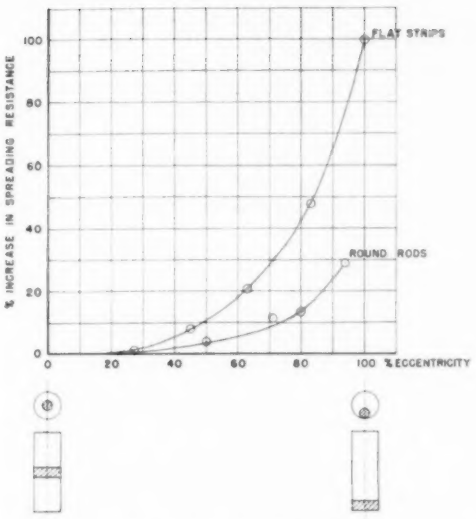

Fig. 10 The effect of the eccentricity of the constriction on the spreading resistance

CONCLUSIONS

The results of this study provide an explanation of the phenomenon and a method of calculating the magnitude of the spreading resistance of single contacts. They show that:

1. The spreading resistance forms an important part of the resistance of contacts.
2. The major portion of the spreading resistance is concentrated close to the areas in actual contact.
3. The spreading resistance is ohmic in nature and is proportional to the specific resistance of the material.
4. The spreading resistance for a given size contact area, located symmetrically, may be calculated with a reasonable degree of accuracy on the assumption that the current flow in the body of the material is confined to a cone which has an angle of 98°.
5. The experimental results check with theory.
6. In a resistance weld with perfect contact the only resistance that exists is the spreading resistance.

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Slope Control in Spot and Projection Welding

Slope control facilitates projection welding of aluminum and reduces electrode pickup in spot welding aluminum and coated steels

by Ivar W. Johnson

INTRODUCTION AND SUMMARY

CONSIDERABLE attention has recently been given to the apparent and possible effect of the initial application of current or "wave front" when making a resistance spot or projection weld. It is the opinion of several investigators and users of resistance welding that, through proper control of the initial application of welding current, the usefulness of this process can be expanded and production difficulties decreased. This paper presents and discusses data pertinent to the use of the "slope control" on spot and projection welding equipment. A summary of these data reveals the following:

1. The slope control can greatly reduce electrode pick-up when spot welding aluminum alloys, thereby increasing tip life and decreasing the time required for tip cleaning.
2. A very important contribution made by the slope control is that it makes possible the projection welding of aluminum. Three types of projection welds have been made successfully:
   (*a*) Round, dome-type projection welds.
   (*b*) Stud welds.
   (*c*) Cross-wire welds.
3. Some improvement is gained in the spot welding of zinc and cadmium-plated steel. The number of spots which can be made before sticking of work to electrodes occurs is approximately doubled, and when sticking does occur, it is not severe.

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Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

EQUIPMENT AND PROCEDURE

The slope control is a supplementary device used in conjunction with standard welder control panels which utilize phase-shift heat control. It permits a gradual increase of the current from any desired value (not under 20%) to full welding current, over a period of time variable from three to thirteen cycles. In effect, the slope control is a variable resistor in series with the heat-control potentiometer. Variation in resistance is obtained by varying the bias voltage of two GL-502A thyratrons connected in inverse parallel. Figure 1 illustrates schematically how the slope control functions; Fig. 2 is a typical current-force oscillographic trace showing how the current rises to welding value.

The electrodes used for spot welding aluminum were Class I of the cadmium-silicon-copper type. Initially, several different dome radii were tried. It was found that a 3 in. radius was satisfactory for all gages of ma-

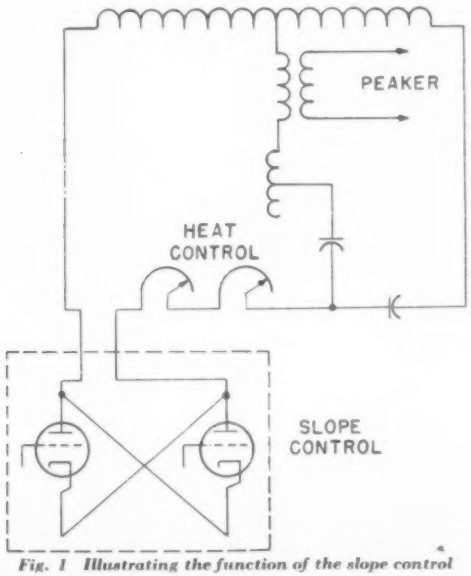

Fig. 1 Illustrating the function of the slope control

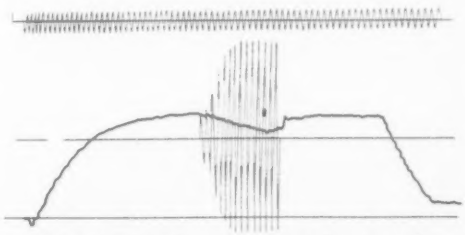

Fig. 2 Current-force trace using slope control

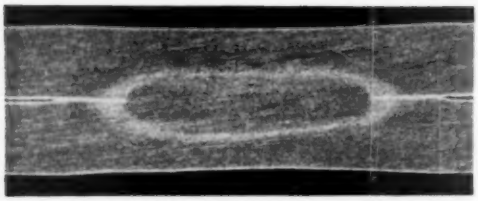

Fig. 3 Cross section of spot weld in 0.064 24ST aluminum. 8 ×

terial and since this is a standard radius for other work, it was used throughout the investigation. For spot welding plated steel, RWMA Class II electrodes with a 3-in. radius dome was used.

The various types of projection welding were done with both Class I and Class II electrodes. There were no apparent differences in their performances.

The cooling water hole was drilled to within $^1/_4$ in. of the welding surface of all spot-welding electrodes. City water at 58° F. circulated through the electrodes at a rate of approximately 1 gal. per minute.

Cleaning the electrodes used for spot welding aluminum was accomplished by immersing the tips in sodium hydroxide solution. This removed all of the aluminum deposit and left the electrodes ready for further use. If pickup was too severe, pitting would appear after dipping and a machining operation would be required. Pickup resulting from spot welding of plated steels is easily removed by rubbing the electrodes with emery or crocus cloth.

The following steps were used to clean the aluminum surfaces prior to welding:

1. Vapor degrease.
2. Alkaline cleaner at 180° F. (82° C.).

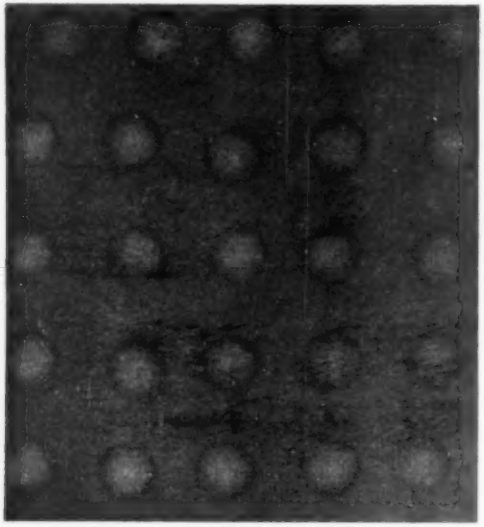

Fig. 4 X-ray of a group of spot welds in 0.064 24ST aluminum. 1 ×

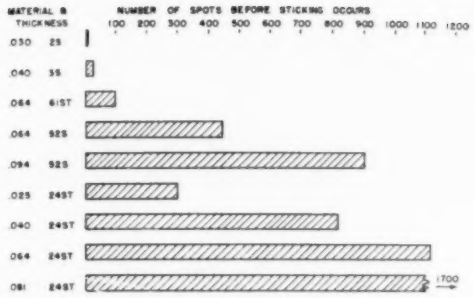

Fig. 5 Results of electrode tip life tests

3. Cold water rinse.
4. 15 to 20 sec. in an acid deoxidant at 70° F. (21° C.).
5. Cold water rinse.
6. Hot water rinse.
7. Dry by air blast.

This procedure produced a surface with a resistance of 5 to 10 microhms on all alloys except 61ST, where surface resistance measured 25 to 35 microhms. Surface resistance was measured by clamping two pieces of the material between two current-carrying electrodes and measuring the voltage drop across the two pieces. Clamping pressure was 1000 lb.

The above process leaves a protective film on the surface of the material. This retards further oxidation and retains a low surface resistance over a considerable period of time. The welding, therefore, was not necessarily completed immediately after cleaning.

Three press-type, air-operated spot welders were used:

1. 300 kva., high-inertia head.
2. 150 kva., air-lock on ram.
3. 50 kva., low-inertia head.

All machines were equipped with synchronous precision electronic controls.

SPOT WELDING ALUMINUM

One serious problem encountered when spot welding aluminum is the relatively short electrode tip life. It is believed that pickup starts from the very first spot and becomes progressively aggravated. Electrode tip life, here, is a function of tip pickup (or deposition of aluminum on the tip) rather than physical deformation of the tip surface such as occurs when spot welding ferrous materials. This part of the investigation resolved itself into a study of the effect of the slope control on electrode tip life.

In all the tests covering the spot welding of aluminum, Air Force Aeronautical Specification AN-W-30 was used as a guide to determine weld quality and strength. Cross-section and X-ray examinations were made to determine the soundness of the spot welds. Figures 3 and 4 are a typical cross section and X-ray photograph.

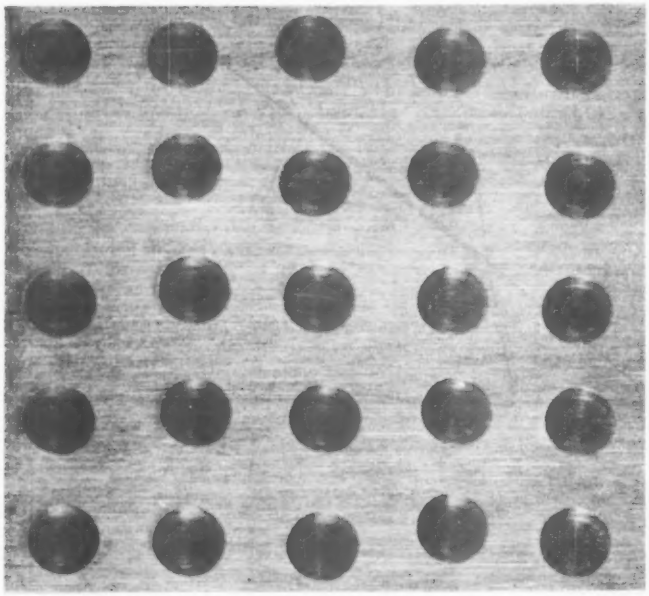

Fig. 6 Showing surface appearance of a group of spot welds in 0.093 in. 52S aluminum. This represents over 200 spots without cleaning electrodes. 1 ×

respectively, of spot welds made on 0.062-in. 24ST aluminum. The welds produced in the test specimens used to determine electrode tip life meet the requirements of AN-W-30. The welding procedures used are tabulated in Table 1.

To obtain comparable welds made without the slope control, the same procedures were followed except for a change in weld time. By reducing this time by two cycles, approximately equal welds were obtained. This would indicate that, for comparable welds, total heat input was approximately the same.

Due to the difficulty of establishing a criterion by which pickup could be measured, it was decided that a test run would be continued until the first indication that the aluminum was sticking to the electrodes. Such sticking is evidenced by the work following the upper electrode upward or by the slightest prying being required to move the work on the lower electrode. The

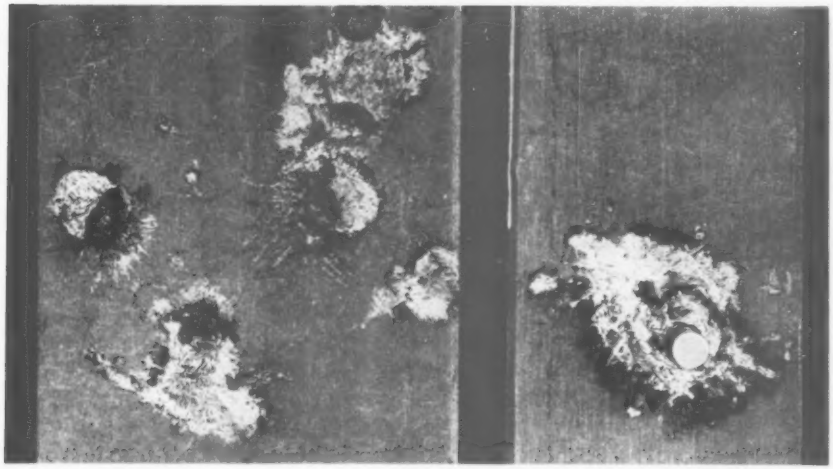

Fig. 7 Illustrating burning of work surfaces when attempting to projection or stud weld aluminum without slope control. 2 ×

Table 1—Procedures Used in Making Electrode Life Tests

Material	*Thickness, in.*	*Current,* amp.*		*Time,* cycles*		*Electrode force*	*Weld strength*
		Start	*Weld*	*Rise*	*Weld*		
3S	0.040	6000	24,000	3	3	700	300
61ST4	0.064	7500	30,000	4	6	1000	1000
52S	0.062	9000	31,000	4	6	1000	900
52S	0.093	8500	34,000	5	9	1070	1400
24ST	0.025	6000	25,000	4	6	700	350
24ST	0.040	6000	28,000	4	6	900	550
24ST	0.064	6000	31,000	4	6	1100	1000
24ST	0.081	8000	40,000	5	10	2500	1400

* Weld time means "number of cycles of welding current." Starting current is calculated from welding current and in all cases is approximately 25% of welding current.

test specimens were only 4 in. square; therefore, sticking did not have to be severe to become apparent. However, without the slope control, when sticking did occur it was usually severe.

The advantages to be gained through the use of the slope control vary with the thickness and the alloy welded. Figure 5 illustrates the effect of these two factors on the electrode tip life. Types 2S and 3S are very difficult to spot weld from a standpoint of tip pickup and, to date, it has not been possible to demonstrate any worth-while advantage of slope control when spot welding these types of aluminum. Although only one thickness each of 2S and 3S is indicated in Fig. 5 other thicknesses have been tried with results similar to those indicated. For welding 61ST, the slope control has some advantage, but not as much as when applied to the welding of 52S or 24ST.

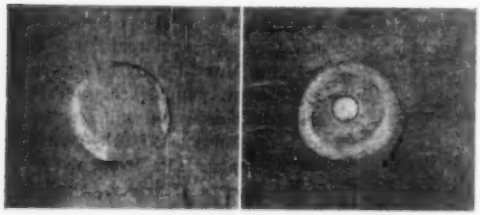

Fig. 8 Illustrating appearance of both sides of work after projection welding 0.062 in. 52S. 2 ×

In the test run on 0.093-in. type 52S, all steps required in An-W-30 were followed. One hundred tensile shear specimens were made at intervals scattered at random throughout the electrode life-test run. Notation was also made of the point where surface marring occurred at the electrode imprint. Although the run continued to 900 spots before sticking occurred, the first surface mar appeared on the 300th spot. Even this, however, is phenomenal performance in view of the fact that, without slope control, surface marring appeared on the 20th spot. Furthermore, when slope control is used, the electrodes are more easily cleaned after the first indication of pickup. Figure 6 shows the surface appearance of spot welds made on 0.093 in. 52S material.

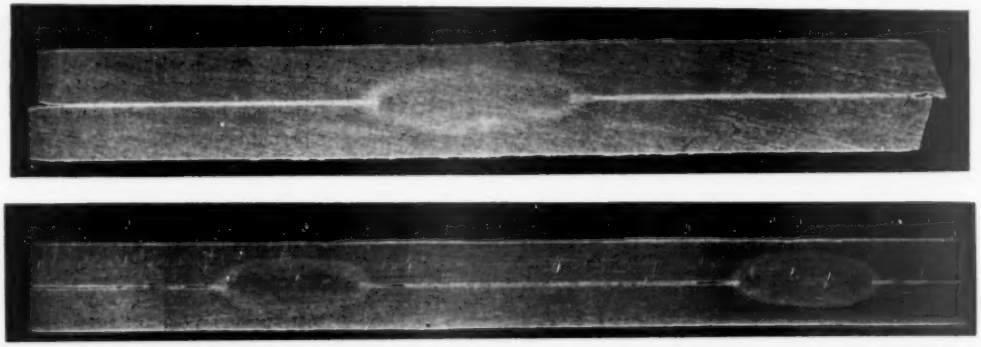

Fig. 9 Cross section of single and double projection welds in 0.064 in. 24ST. 7 ×

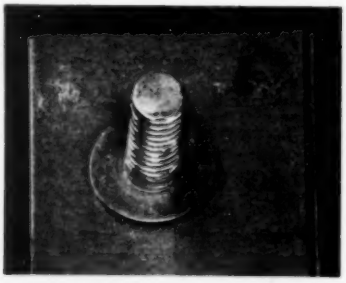

Fig. 10 2S aluminum stud welded to 52S aluminum sheet. 2 ×

PROJECTION WELDING ALUMINUM

In the past, the various forms of projection welding have not been successfully applied to the fabrication of aluminum. Because of the difficulty encountered in consistently making sound projection welds and the

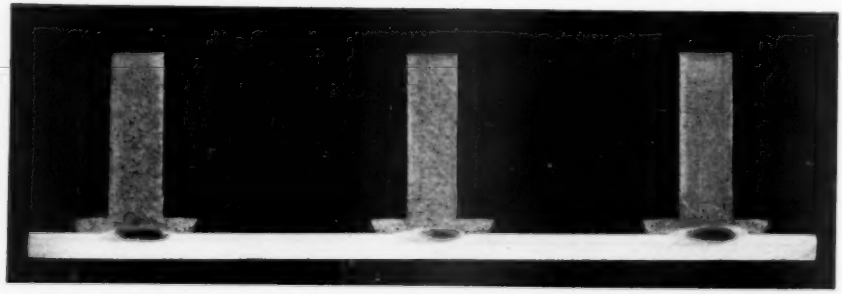

Fig. 11 Cross section of 2S aluminum stud welded to 52S aluminum sheet. 2 ×

Table 2—Projection Welding Procedures

Alloy	Thickness, in.	Current, amp. Start	Current, amp. Weld	Time,* cycles Rise	Time,* cycles Weld	Force, lb.	Projection Size Height	Projection Size Diameter	Weld strength
			Single Round-Dome Projections on Flat Sheet						
62ST4	0.064	10,000	40,000	9	18	700	0.035	0.156	1000
61ST4	0.091	12,000	46,600	6	22	800	0.041	0.187	1500
52SO	0.093	10,000	38,800	6	14	920	0.041	0.187	1600
52SO	0.125	11,000	44,000	5	20	125	0.041	0.187	2300
24ST4	0.125	11,000	44,000	7	20	1300	0.041	0.187	2500
24ST4	0.064	7,000	27,600	9	20	500	0.028	0.119	940
			Double Projections						
24ST	0.064	11,000	44,000	11	19	750	0.028	0.119	2000
62ST	0.064	12,000	48,000	9	18	750	0.035	0.119	1400

* NOTE: Weld time means "number of cycles of welding current." Starting current is calculated from welding current and in all cases is approximately 25% of welding current.

necessity for specially designed projections, this process has not been generally used in industry. Figure 7 shows one of the difficulties encountered when an attempt is made to projection weld aluminum using the conventional round dome or stud. Here are shown the surfaces of the work after attempting to make a multiple projection weld or a stud weld without using the slope control. It is evident that there is severe expulsion and burning of the work surfaces; this is caused by the extremely high current density during the first half-cycle of welding current when the projection is contacting on a very small area of the plate relative to its base area (or area of the completed weld). The extremely high current density causes part of the projection to become molten and be expelled, allowing a total loss of pressure and resulting in arcing between the workpieces and between the work and electrodes.

When the slope control is used, this condition is eliminated entirely and it is possible to make good, sound welds with excellent surface appearance. Figure 8 shows the work surfaces as they appear after projection welding. The indentation produced during the forming of the projection is completely filled in, leaving a smooth surface; the opposite surface has a minimum indentation—approximately 0.001 in. Figure 9 shows cross sections of single and multiple projection welds in 24ST alclad. These sections reveal sound, well-formed welds free from internal defects and any expulsion between the sheets. Table 2 is a tabulation of welding procedures, projection sizes and weld strengths. The projections used were similar to those used on ferrous metals, but it is very evident that projection size should be smaller on aluminum than on a corresponding thickness of ferrous material. When forming projections on aluminum, care must be exercised to avoid over-stressing the material as this would cause cracking during forming or later, when the weld is made.

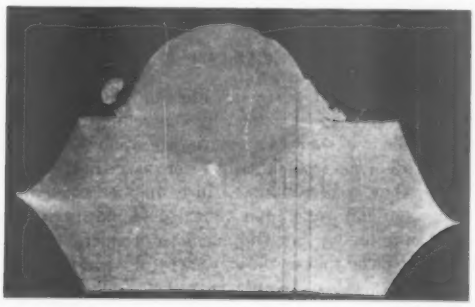

Fig. 12 Cross section of 1/8-in. 2S aluminum cross wire weld. 5 ×

A variation of round-dome projection welding is referred to as stud welding. The stud welding of ferrous materials has been common practice for some time; welding of aluminum studs to aluminum sheet, however, has been too difficult and produced results too erratic to make it a successful production process. Figure 7 shows the results of an attempt to stud weld without the slope control.

The slope control makes it relatively easy to stud-weld aluminum. Common 2S aluminum round-head rivets were used in all of the tests performed here; these rivets are commercially available and required no special preparation before welding. Figure 10 illustrates the appearance of a 2S stud welded to 52S sheet. Figure 11 is a cross-sectional view of stud welds. To permit tensile testing, the shanks of the rivets were threaded to provide a means of gripping them. In all tensile tests, the rivet failed before the weld, and when the weld was subjected to the peel test, a hole was pulled in the sheet. Table 3 shows welding procedures and strengths obtained for three sizes of studs.

A third variation of projection welding is the cross-wire weld. This form of welding has been applied extensively to ferrous material for a long time. Cross-wire welding of aluminum, however, has not been used because of the difficulty of making such welds—violent expulsion of metal would usually occur when such welds were attempted, and although there may be isolated in-

Table 3—Welding Procedures for Stud Welding Aluminum

Material—Stud 2S—Sheet, 0.094 in. 52SS

Stud size, in.	Current,* Start	Amp. Weld	Time,* Cycles Rise	Cycles Weld	Force, lb.	Weld strength
1/8	5000	21,000	13	17	250	150
3/16	6200	31,000	13	17	600	375
1/4	8800	44,000	13	17	850	600

* Weld time means "number of cycles of welding current." Starting current is calculated from welding current and in all cases is approximately 25% of welding current.

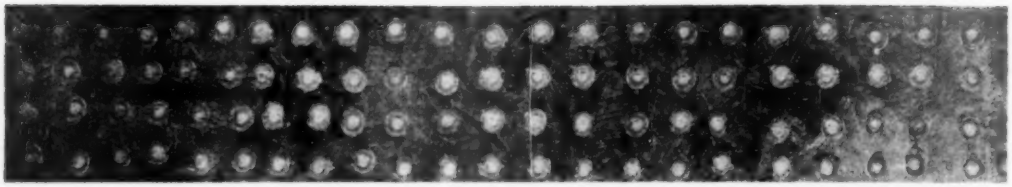

Fig. 14 A group of spot welds in zinc-coated steel. 1 ×

Table 4—Welding Procedures for Cross-Wire Aluminum Wire

Wire diameter, in.	*Material*	*Current, amp. Start*	*Weld*	*Time, cycles Rise*	*Weld*	*Force, lb.*
1/8	28	4500	13,500	4	4	165
1/4	28	6250	25,000	3	5	200

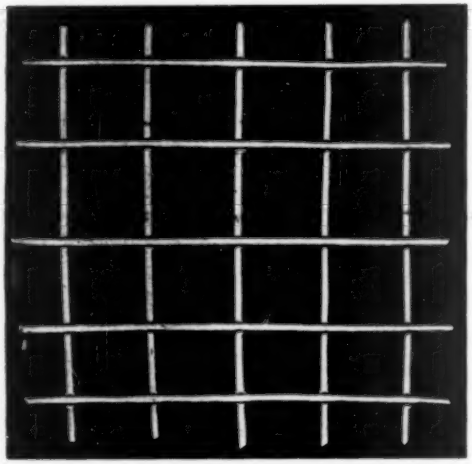

Fig. 13 A series of cross wire welds showing appearance as welded. 1 ×. Reduced 40% in reproduction

stances where this process has been used, it is generally conceded that it is impractical from a production standpoint. The slope control makes this weld relatively easy to accomplish, and consistently sound welds can be made. As evidenced by Fig. 12, (a typical cross section), good fusion occurs over the entire area. Figure 13 shows appearance of a series of cross-wire welds. Welding procedures are tabulated in Table 4.

SPOT WELDING OF PLATED STEELS

Cadmium or zinc plating is frequently used as a corrosion preventative on carbon steels. It is not advisable to plate the metal after welding due to the danger of trapping acids in the joint, and it is therefore necessary to plate the component parts before assembly and welding. When spot welding is used, considerable difficulty is encountered with the transfer of the plating to the electrode tip. To make a satisfactory weld on plated steels, it is necessary to use relatively shorter time and higher current than when equivalent materials

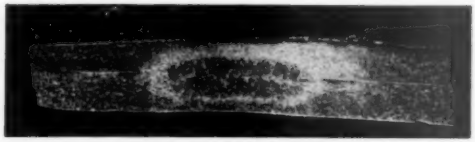

Fig. 15 Typical cross section of spot weld in zinc-coated steel. 8 ×

without plating are to be welded. The high current melts the low-melting-point plating metal at the interface and permits it to be squeezed out; the weld is then completed between the steel faces. The short weld time minimizes the tendency of the plating metal to alloy with the steel in the weld. In addition, the plating after being squeezed out, solidifies upon reaching the cold metal surrounding the spot and retains the protection around the weld.

The higher current densities resulting aggravate the tendency of the plating metal to alloy with the electrode. The slope control, however, permits the electrode tip to become properly seated on the work before full welding current is reached and thus it eliminates extreme current densities at the point of contact between the electrodes and the work. This minimizes pickup, but with low-melting-point plates it is impossible to eliminate it entirely. On cadmium-plated steels it was found that 150 welds could be made with the slope control before mild sticking occurred, while without slope control, sticking occurred on the 60th spot. On zinc-plated steel, over 200 spots were welded without evidence of sticking when the slope control was used. Figure 14 shows the surface appearance of spot welds on zinc-coated steel; Fig. 15 shows an etched cross section of one of these welds. Tensile-shear specimens taken periodically during these tests revealed consistent weld strengths.

CONCLUSIONS

The data presented here definitely indicate that there are advantages to be gained by controlling the initial application of current to a spot or projection weld. Although all of the tests described were made in the laboratory, they were conducted in a manner simulating production-line work. No difficulty should be experienced in applying this equipment to production work.

It is believed that as the slope control is put into production use, other advantages will be found and the user will be able to produce better welds more easily, with consequent improvement in product quality.

Spot-Weld Consistency Studies

Uniform high quality welds can be successfully produced by following a prewelding control procedure. This involves proper design, uniform material, proof-tested welding schedule and tested equipment and controls

by J. Heuschkel and H. Bitzer

INTRODUCTION

The production of consistent spot welds to a specified quality standard, usually a minimum strength requirement, with complete assurance that such welds have been secured, is a necessary objective of welding supervision and quality control personnel. Good welds are made in the commonly welded metals by using the proper conditions. It is a matter of concern and economic loss when production plants do not assure themselves that good spot welds are being consistently obtained without the use of subsequent expensive inspection and repair.

The primary purpose of this paper is to demonstrate that uniformly high-quality welds can be successfully produced by following a prewelding control procedure. For a properly designed part this involves knowing in advance, from inspection testing, that the work material is uniform in its characteristics; that the proof tested welding schedule to be used is capable of producing welds of the desired properties in a consistent manner; and from actual measurement that both the equipment and controls are providing the conditions required by the operating schedule. Also it is necessary to know in advance the limits of permissible deviation from the set-up schedule so that the necessary instrumentation for detection of variations from the desired welding conditions can be intelligently evaluated. Obviously an operating range is necessary. A periodic checkup on weld quality itself need involve only the most inexpensive form of peel or separation tests as conforms to reasonable inspection practice. These tests can be made quickly on the floor.

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This paper is scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

Adequate instrumentation in conjunction with established and proof-tested schedules can be employed to show when the limits are about to be violated before the actual inconsistencies occur. This procedure is shown to be superior to the chart method of statistical control. Since the properties of spot welds may vary with either changes in welding conditions or with material characteristics, the production of consistent welds requires that both sets of variables be subject to control. When both conditions are under control the quality uncertainties vanish and the need for subsequent expensive inspection is greatly reduced or eliminated.

The second purpose of this paper is to review the causes for inconsistent spot welds, using a. c. as an example. From such a study some causes for the inconsistencies become apparent. The important variable is the condition under which the welds were made and not whether the welds are made in the laboratory or on the production line.

A third purpose is to illustrate the use and some deficiencies of the statistical control chart methods for evaluating production quality, while explaining the basic reasons for the variations obtained.

STATEMENT OF PROBLEM

Spot welds are made by passing electric current through locally compressed portions of the overlapped work parts for a sufficient period of time to melt the base metal, without overheating it. Electrodes clamp the work material together during, preceding and following this interval. The magnitudes of the current, time and force and the electrode size, shape and composition are caused to vary with the thickness, composition, temper and surface conditions of the parts being welded. Machines and controls are available for supplying the necessary pressure and heat energy required. For the insensitive metals, such as low-carbon steel, it has been well established that the work parts may be effectively mechanically clamped between the impressing electrodes by the use of a range of electrode sizes, shapes and forces. If too much current is passed for too long a period the locally compressed metal will be overheated, and expulsed from the weld zone in the form of a molten stream. This is a readily visible phenomenon and the resultant welds are often cracked, distorted and over indented. If, on the other hand, the magnitude of the current is reduced too much the metal may externally appear to be heated to the proper degree, from the operator's viewpoint, and yet the welds made will have inadequate strength as the result of the too low internal temperatures achieved. As current is further reduced, or if some of the available current is shunted through previously made welds, ultimately no weld is produced even though automatic sequencing controls may be properly functioning but providing inadequate current at the point where it is needed. In the absence of installations wherein controls pass the correct amount of energy at the necessary rates it is apparent that a system of rigorous schedule regulation must be enforced if consistent spot welds of the desired strength are to be produced. Too large variations in welding time or force; in electrode size, shape or composition; and in work material characteristics and consistencies also influence spot-weld properties and consistencies. For prewelding quality control one must therefore determine a suitable schedule, the permissible limits of deviation from that schedule and then provide means for knowing what the machine and controls are delivering to the weld nugget.

TEST METHOD

For illustrative purposes the present report is confined to stainless and low-carbon steels. The same principles apply to other metals with detail variations. Standard tension-shear tests were made, and the average group strength, the statistical standard deviation from the average, and this deviation expressed as a percentage of the average, called the coefficient of variation, were obtained for each condition investigated. This procedure was used because entire systems involving different weld strengths were to be compared. Such comparisons cannot be made directly

on a basis of strength, or upon the standard deviation which may become large in value with high strengths. A low coefficient of variation indicates a high order of consistency; a zero value would represent absolute uniformity. The standard deviation is calculated by taking the square root of the difference between the average of the squares of the individual values and the square of the average value. The ratio of this derived value to the average obtained, expressed as a percentage, is used throughout as the coefficient variation and as a measure consistency.

In the investigations being reported static welding force only was measured. This was done with a calibrated force bar. The 50-kva., air-operated press-type spot welder used was subject to fluctuations of air line pressure. Time was controlled by a synchronous electronic control and was measured with a clock-type cycle counter, calibrated for accuracy against a magnetic oscillograph. Secondary current was measured from an indicating meter with a signal picked up from an iron core inductor placed over the lower arm of the secondary loop. While the accuracy of this method is open to some question it is believed that the reported values are comparable, at least for illustrative purposes. Many engineers prefer to obtain the magnitude of the secondary current by multiplying the pointer stop ammeter primary current determination by the turns ratio. Current was varied by transformer tap settings and a phase shift electronic heat control.

As a starting point a series of 95 specimens were first made and tested in tension shear to demonstrate that a previously established schedule[1] was adequate for making consistent welds in 0.095-in. thick low-carbon steel. For welding two pieces with single or widely spaced spots this schedule called for the use of:

Force	1,050 lb.
Current	11,400 amp., r.m.s.
Time	25 cycles (60 c.p.s.)
Electrodes	0.26 in max. end $\times$ 4° face taper

On another material lot of the same thickness, a series of 20 specimens were then made and tested to demonstrate that the same schedule would again produce consistent welds, although the average obtained strength may be different due to differences in material characteristics. This over-all procedure fulfills one specification requirement that process approval must be obtained through a demonstration of equipment performance.

A continuing series of forty-two 3-specimen groups was next prepared with a willful change from the established schedule after every third group. Immediately after each such variation the machine-electrode-control system was brought back to the pre-established schedule: then another set of three 3-specimen test groups was made. Strength data from these specimens provide a groundwork for the use of the control chart system,[2] using statistical methods. Sufficient supplementary data were also obtained for one material to show why and how much variation is obtained in both strength and consistency by off-schedule welding. The compiled graphs of resulting strengths clearly indicate that had the welds been made on an actual production line run, the existence of indicating instruments on the welding variables would have prewarned the operators before off-quality welds would have been produced.

Data from a previous paper[3] and new data for various electrode shapes and steel tempers were also used in consistency calculations to provide a basis for demonstrating how to pre-evaluate the relative suitability of various welding schedules over a thickness range.

DISCUSSION OF WELDING VARIABLES

Force

The influence of variations of force only, upon weld strengths and consistencies, are shown in Fig. 1 for four 3-specimen lots. The best value for the particular material, from the production viewpoint, would have been 850 lb., since with that value a plus or minus variation of as much as 30% in force magnitude would not have materially influenced weld strength or consistency. Forces exerted as the result of cam or spring actions or from air or hydraulic pressures can and do vary somewhat with the source magnitudes or frictional values. The significant new point is that the best consistency was obtained with welds of maximum strength and within mid-range of the operating conditions for producing those strengths.

Sometimes production is done at the low side of the force range. With such schedules any transient further reduction results in poor welds. From Fig. 5 it is apparent that forces under 1000 lb. for the particular material and electrode combination, result in rapid changes in initial contact resistance, and thus may tend to accentuate surface heating.

Time

The general influence of time of current

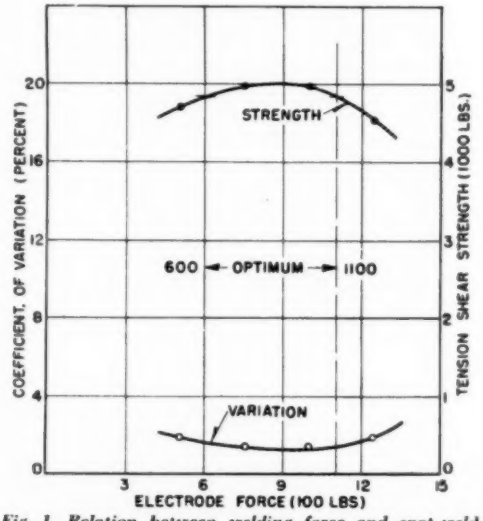

Fig. 1 Relation between welding force and spot-weld strength and consistency (0.095-in. low-carbon steel)

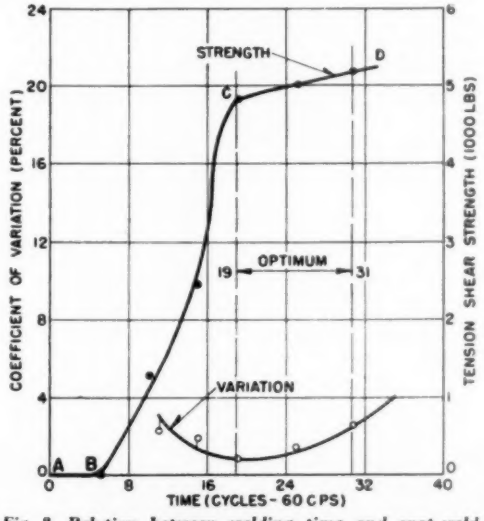

Fig. 2 Relation between welding time and spot-weld strength and consistency (0.095-in. low-carbon steel)

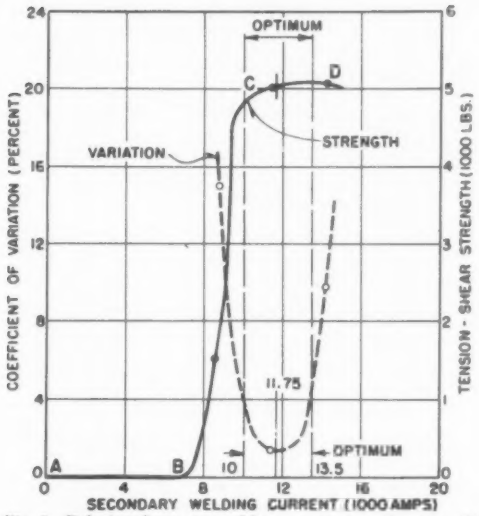

Fig. 3 Relation between welding current and spot-weld strength and consistency (0.095-in. low-carbon steel)

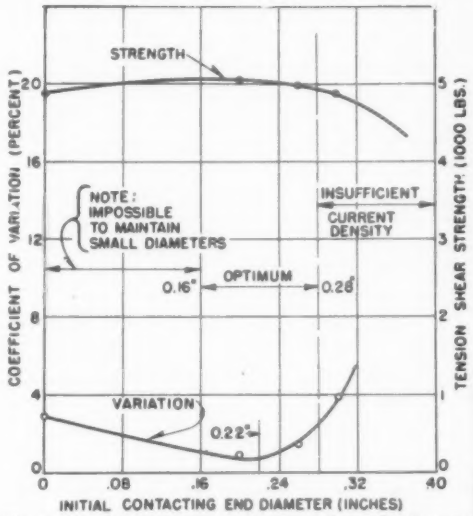

Fig. 4 Relation between contacting electrode diameter and spot-weld strength and consistency (0.095-in. low-carbon steel)

flow upon weld strength and consistency is illustrated by Fig. 2 for the otherwise standard schedule. Starting from the origin it is known that a discrete period of time is required to initiate the formation of a weld nugget. For the very short times no adherence is obtained between the two pieces. As time is increased a small nugget is formed. Weld strength then improves rapidly with further increases in time, after which still further increments of time result in little additional strength. The precise shape of this time-strength curve depends upon the specific material and the force and current magnitudes. Figure 2 may be taken as a type relationship. Too much time slows down production, increases surface heat to the extent that electrode life is reduced and may cause weld interface molten metal expulsion. The optimum time from the viewpoint of both weld strength and consistency is within the *C* to *D* range, Fig. 2. For the 0.095-in. stock under consideration, this is between 19 and 31 cycles for the force-current conditions used, which is the range for producing the strongest welds with the least variation between individual values within each group.

Current

The general influence of current only, upon weld strength and consistency, is shown in Fig. 3 for the same material, when using an otherwise standard schedule. The current-strength characteristics are similar to those shown in Fig. 2 for influence of time upon strength, except that they are more pronounced. This is in line with the heat equation which shows time to be a first power and current to be a second power variable. Within the *A–B* range in Fig. 3 no weld is formed. Within the *B–C* range abrupt changes in strength occur with small variations in current and within the rather narrow *C–D* range is therefore the optimum operating range from the standpoint of resultant strength and consistency. Line voltages are subject to fluctuations and these changes cause currents of different values to pass through the welding electrodes for a given machine and heat control setting. A schedule employing currents just less than the *C* value in Fig. 3 could not tolerate any current loss without complete loss of weld strengths. The use of an 11,750 amp. value, however, permits a plus or minus 15% current fluctuation without any appreciable variation in weld strength. The mid-range current also produced welds of maximum consistency. Currents higher than the value *D* cause overheating, molten metal expulsion, distortion and often cracking.

The current magnitude is the spot welding variable which must be most carefully controlled, because of the second power effect in the heat-generating equation. Even so, it is demonstrated by Fig. 3 that it is possible with such metals as low-carbon steel to preselect a schedule sufficiently insensitive to variations to permit a plus or minus variation of 15% in current magnitude while maintaining full strength of the welds and with each subgroup consistent within itself to less than 5%.

Electrodes

The influence of variations in electrode face dimensions only, are shown in Fig. 4, for the standard force time and current conditions. These particular data are restricted to the 4° taper; since it is only with low-angle tapers that such small initial point contacts could be used. For all small-end electrodes, either conical or spherical in shape, the high-unit pressures on the contacting ends tend to deform the electrodes after a few welds. The values given are the strengths and consistencies prior to electrode deformation as would occur under extended usage. For the dimensions used, values less than the specified initial 0.26-in. electrode end diameter maximum produced strong consistent welds, but such electrodes would have a short operating life. The use of a too large electrode area results in insufficient current density (current per unit area) to bring the metal between the electrodes up to the welding temperatures.

The question of the most desirable electrode shape is a common one. Four combinations were investigated. The first involved two 3-in. spherical radius electrodes in combination, and the second used one 3-in. spherical radius with one flat across the entire body diameter. The third combination was with two identical 20° face taper ends having a suitable flat end and the forth combination involved two identical 4-in. face taper ends with suitable flat contact diameters. Figure 6 shows that for the particular schedules employed, all shapes tended to give the least consistency on the thinner sheets. For the thicker sheets all shape combinations investigated were equally satisfactory from the standpoint of producing a series of 25 consecutive welds having a group consistency variation of less than 7%.

It should be noted that the lesser consistencies (higher coefficient of variation) obtained from the 4° face electrodes on the thin sheets were partially the result of the

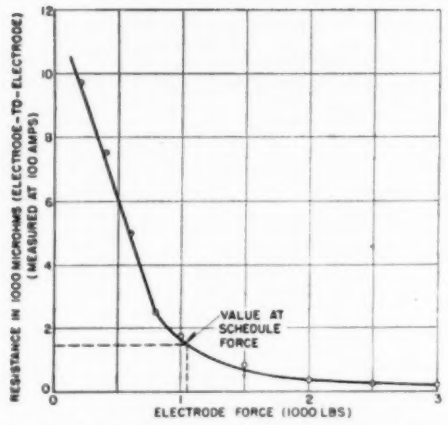
RESISTANCE IN 1000 MICROHMS (ELECTRODE-TO-ELECTRODE)
(MEASURED AT 100 AMPS)
VALUE AT SCHEDULE FORCE
ELECTRODE FORCE (1000 LBS)

Fig. 5 Relation between force and contact resistance

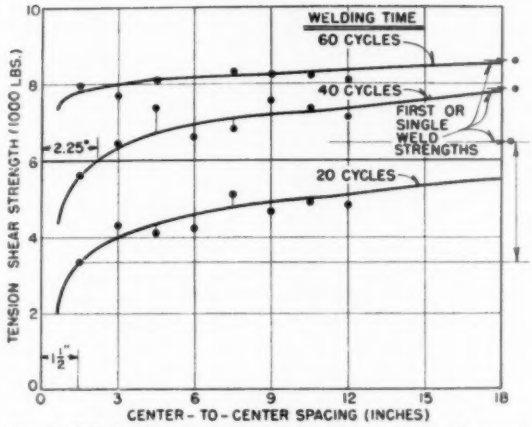

Fig. 7 Relation between spot-weld spacing, welding time and tension-shear strength (0.125-in. thick low-carbon steel)

lower pressures and shorter weld times. As thickness of material increases there is a tendency for decreasing consistency. Not all of these increases in consistency variation at the extremes of thickness for any of the four Fig. 6 schedule and electrode shape combinations tested are chargeable exclusively to the electrode; the increases at both the extremes being partially the result of the force-time-current combinations used.

The combination of a 3 in. radius with a flat electrode to produce one approximately flat final exposed surface on the finished two-ply joint is shown to be suitable across the range up to $^1/_8$ in. in thickness. This combination is desired in some shops because of ease of alignment.

Weld Spacing

All the preceding illustrations were for single welds. Production joints are made containing multiple welds. Depending upon conditions, more or less, variation is found in the results as current shunting losses vary. Figure 7 illustrates the results obtained with one schedule[1] on $^1/_8$-in. plate for three different welding times, all other conditions being constant. These tests were made by removing one welded specimen after each weld, following the first, was completed. While each point in that figure represents the strength of a single weld it is obvious that the consistencies of the 60-cycle welds within the joint would have been good while that of the 20-cycle welds would have been poor, if the comparison was made upon a series of joints of the same materials involving different weld spacings. In this case the 60-cycle welds would also all have had higher strengths per weld then the 20-cycle welds.

The influence of increased welding time required to compensate for shunting losses as the result of close weld spacings is also shown by Fig. 7. If a setup is desired, for example, to provide a test control strength of 6000 lb. minimum per spot, it can be shown that this strength may readily be obtained or exceeded for single welds with times of from 20 cycles and more, as on the right of Fig. 7. For production speed a selection of 20 cycles would be desired. However, the data show that with this time even at 12-in. centers the minimum strength per weld of 6000 lb. cannot be obtained. Using a 40-cycle time, spacings as low as 2.25 in. can be used without providing values below 6000 lb. per weld but welds made at variable higher spacings will not be consistent among themselves. Strengths ranging from 6000 to 8000 lb. per weld must be anticipated for such time-spacing combinations. When using a 60-cycle time schedule higher strength welds are obtained throughout and there is very little variation in weld to weld strength, regardless of spacing down to $1^1/_2$ in. Only limited corrections for current shunting can be made by increases in current magnitude because of surface overheating.

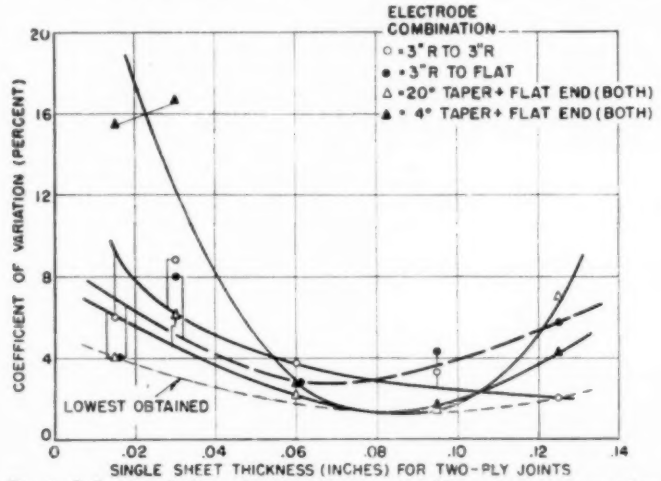
ELECTRODE COMBINATION
○ - 3" R TO 3" R
● - 3" R TO FLAT
△ - 20° TAPER + FLAT END (BOTH)
▲ - 4° TAPER + FLAT END (BOTH)
COEFFICIENT OF VARIATION (PERCENT)
LOWEST OBTAINED
SINGLE SHEET THICKNESS (INCHES) FOR TWO-PLY JOINTS

Fig. 6 Influence of electrode contour on spot-weld consistency (low-carbon steel)

DISCUSSION OF MATERIAL VARIABLES

Temper

It has been established that spot-weld tension-shear strength for a given weld nugget diameter increases with increased strength of the carbon steel as a given composition is work hardened.[1] Also, while composition or hardness of such steels are increased the direct tensile strength of the welds tends to decrease. Figure 8 shows how strength and consistency were found to vary when spot welding two thicknesses of 0.05-in., Type 302 stainless steels of varying hardness, when using a schedule suitable for the softer tempers. Welding of the harder sheets was accompa-

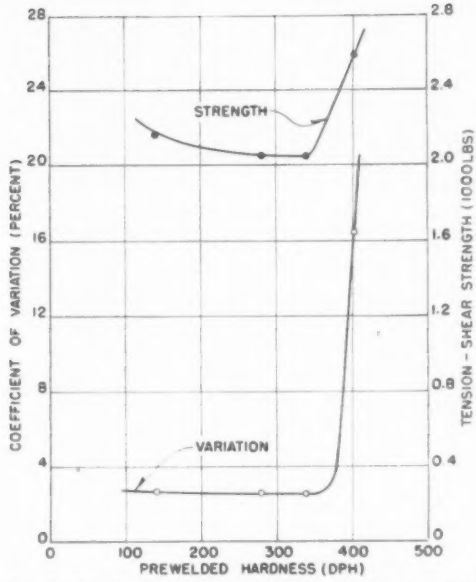

Fig. 8 Relation between prewelded 0.05-in. stainless steel hardness and strength and consistency of spot weld at one setting

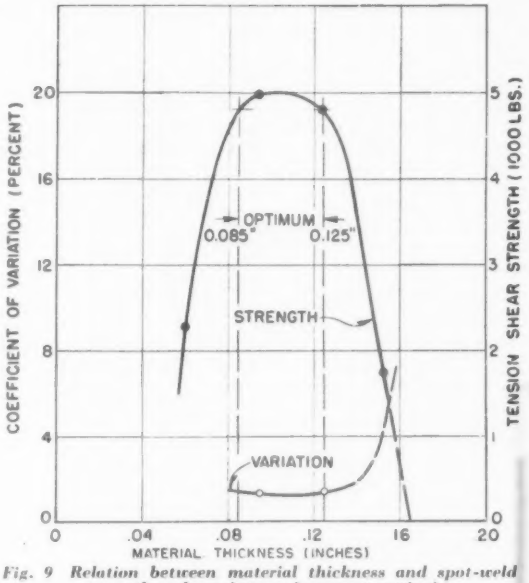

Fig. 9 Relation between material thickness and spot-weld strength and consistency (constant setting)

nied by metal expulsion and the conditions used were unsatisfactory for both operating and consistency reasons. For annealed, 1/4 and 1/2 hard tempers the previously established schedule consistently produced welds having a coefficient of variation of less than 3%, as shown in Fig. 8. The same schedule, when used on the full-hard grade of stainless steel of the same composition resulted in the production of stronger but less uniform welds. The electrode force was not sufficient to restrain the molten metal from expulsion and the electrode material used was not sufficiently hard to withstand the hot extrusion pressures potentially present as a result of the sheet hardness variations between the different tempers. The established schedule is therefore suitable for three tempers (annealed, 1/4 hard and 1/2 hard) but is unsuitable for the full hard temper, in spite of the fact that it produces stronger welds on the latter material.

Thickness

It is common practice to increase the magnitudes of the force-time-current variables as material thickness is increased. As a result, the weld strength increases with thickness in a parabolic relationship. When these adjustments are not made, results as depicted by Fig. 9 are obtained. As the material thickness and mass increase above the optimum value for the particular schedule the weld strength approaches zero as no weld nugget is formed. As the thickness decreases below the optimum the strength also rapidly diminishes and the confined zone may become badly overheated locally causing the electrodes to deteriorate rapidly.

Figure 10 shows the average consistency obtained from a series of schedules, when applied across a thickness range from 0.015 to 0.50 in.[3] Each point is the coefficient of variation for a 5-specimen subgroup. Each subgroup was made from a different lot of steel containing less than 0.20% carbon and included sheet, bar and plate stock. For most of the 23 lots of steel, spot welds were being made with a group variation of less than 5%, an indication that welding variables had been properly compensated, on the average, to correct for material thicknesses up to 0.33 in. Figure 10 is an example of consistency studies applied to system preselection and control.

CONTROL CHART SYSTEM

The control chart method of evaluating final production quality consists of plotting a continuous record of the determined values, which are of interest, against a time or group number abscissas.

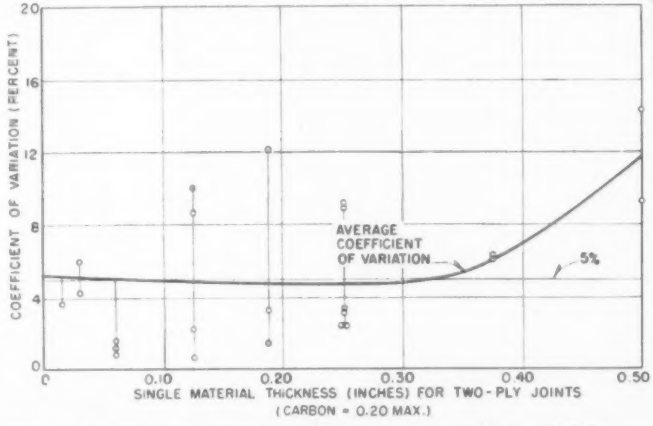

Fig. 10 Application of variation coefficient to spot-welding schedules over range of thicknesses

For spot-weld control the tension-shear strength value is usually the object of interest. In the example shown by Fig. 11, 95 consecutive spot welds were first made and tested to "qualify" the machine, and control. The results of these tests provided the first point *Q* on the charts. Later, using a different lot of steel, but the identical schedule, a series of 20 consecutive individual test specimens were prepared to demonstrate that the setup and schedule to be used was satisfactory. The average strength and consistency of this group of specimens is given by the second point *O* in Fig. 11. The original tests on the 95 specimens averaged 5175 lb. per spot; the calculated standard deviation was 71.5 lb. or 1.4% of the obtained average. The second group of 20 specimens averaged 5015 lb. per specimen; the standard deviation was 80.7 lb. or 1.6%. Both groups of tests were consistent within themselves to 1.6%. The second group average was only 96.8% of the first group, which is the result of variations in steel between the two lots. This magnitude of variation is representative of a reduction from a 50,000 psi. strength to a 48,400 psi. strength, both steels being within possible specification limits. The variation of spot-weld tension-shear strength with variation in steel tensile strength and analyses has been established previously.[1, 2]

For purposes of illustration, prior to making the specimens for subgroup 4, the air pressure on the cylinder was deliberately increased 25% to provide a static force of 1250 lb. Following the general trend of variation, Fig. 1, for the schedule in use, the strengths obtained from this setting were reduced to the extent that the average values were outside of the subsequently established control limit, although within the group of three specimens the agreement was good. Had a higher force been applied even lesser strengths would have been obtained. Another factor would enter into production consistency under this variation; that is, with the resulting high unit pressures the electrodes would not maintain their shape but would expand in contact areas as the end mushroomed, with a resultant decrease in current density. Had this been an actual production run the use of the control chart would have shown at the end of the one time interval that the results were out of control and the situation could then have been corrected, but an earlier force magnitude check-up would have shown the same thing more quickly.

A deficiency of the control chart method is that it cannot correct for variations occurring between the time intervals at which control checks are made. When using established welding schedules involving known variations in result with any given variation in conditions, a visual, recording or automatic limiter which would indicate departures from schedule or adjust or terminate operations when any particular variable went outside of its known permissible limit, would be superior to the control chart method since it would provide a means for keeping within the required operating limits. For example, when following the pre-established optimum limits for force strong consistent welds would be made, without further testing, provided a similar check-up was being obtained for the other variables.

Immediately upon readjustment to the proper force, suitable welds were again produced, and the subgroups 4A, 5 and 6 were acceptable. Just prior to the making of subgroup 7 the welding force was deliberately reduced by 25% to 750 lb. In accord with the trend of variation for the results obtained, and in the absence of adequate instrumentation, this condition could not be immediately detected by the quality control method. In time, the use of such a low force would probably be detectable from the results produced because

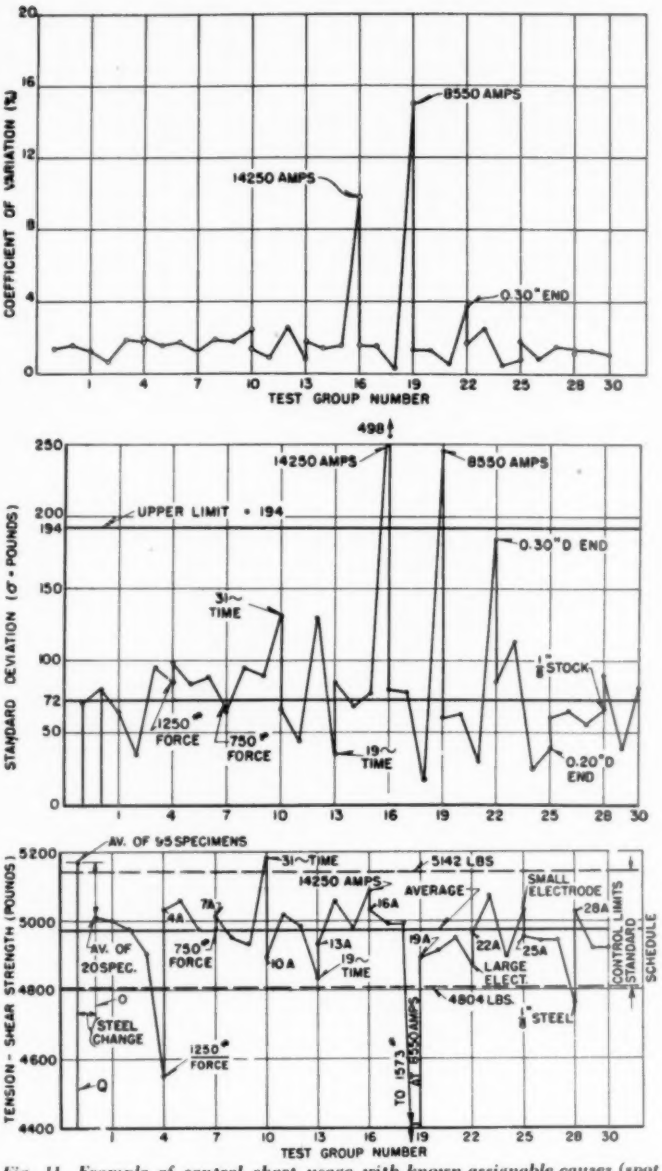

Fig. 11 Example of control chart usage with known assignable causes (spot welds in low-carbon 0.095-in. steel with Schedule "O" except as noted)

the lower force would result in lower unit pressure and higher contact resistance between the electrodes and the work, as shown in Fig. 5 for this particular combination. This in turn results in more serious surface heating and electrode deterioration. After again returning to the initial schedule, subgroups 7A, 8 and 9 provided normal acceptable results.

The time control was next preset off schedule plus 25% to provide 31 cycles for the making of subgroup 10 with the results shown. Considerable scatter within the subgroup of 3 specimens was obtained and the strengths were out of control on the high side in accord with the trend of variation for time, as shown in Fig. 2. Lowering the time to 19 cycles in subgroup 13 reduced the strength, also in accord with Fig. 2. The results were still within the established control limits for normal operating conditions for times as low as 18 cycles for the single-weld specimens. Whether or not any particular operating schedule can satisfactorily withstand a time reduction as much as 25% is a function of the schedule and the weld spacing. For example, using all other conditions the same, if an 18-cycle time period would have been selected as a normal schedule, a 25% reduction in time would have, on the average, reduced the strength to 2800 lb. which is an unpermissible amount.

Groups 16 and 19 wherein the current magnitudes were varied first plus and then minus 25% are of particular interest and significance. The increase in current did not materially alter the average subgroup strength but the consistency within the group was not good. Some expulsion was obtained and this contributed to the erratic results. In practice continued operation at the higher current levels would accelerate the deterioration of the electrodes and increase the irregularities in the welds. The reduced current subgroup produced welds much lower in strength, as shown by Fig. 3, and the shear values were outside the control limits, as shown in Fig. 11. The strengths were also inconsistent among themselves as was to be anticipated from the shape of the current-strength curve in Fig. 3.

Subgroups 22 and 25 were made with over and undersized contacting ends on the electrodes. Neither change resulted in large variations in weld strength although the larger ended electrode produced the more inconsistent results. It is possible to use the electrode shape involved with small end diameters, although extremely small diameters cannot be maintained in production because the high pressures tend to cause mushrooming or enlargement of the initial size from plastic flow.

Subgroup 28 was made with two pieces of 0.125-in. thick, low-carbon steel. The established schedule was not intended to be adequate for this thickness and the results are out of control on the low side. The weld strengths made with the same schedule across the 0.060 to 0.125-in. thickness range provided data for the curves in Fig. 9. Material variations definitely enter into the consistency control problem. For a specific schedule some variation in thickness is permissible but usually such thickness ranges are small.

The control limits set up in Fig. 11 ignore the deliberate variations in schedules imposed for purposes of illustrating the effects. The averages are based upon the 30 group observations. The limits are established following standard procedures.[2]

SUMMARY AND CONCLUSIONS

A series of tests were conducted to show how both the strength and consistency of spot welds vary with welding and work material conditions. Best consistency is usually obtained when welds of maximum strength are being produced. Advantage can be taken of this fact by establishing production schedules within the range of best strength and consistency. The determination of this range provides data on operating limits. The provision of suitable instrumentation to assure that the limits are not being violated can be taken as an assured method of welding control.

The most consistent spot welds are produced when the welds are of maximum strength and the welding conditions are within mid-range for producing those strengths.

The basic reasons for the production of inconsistent spot welds are related to the use of sensitive schedules, too-wide variations in the welding conditions and insufficient allowance for current shunting through prior welds. There are others, such as surface conditions, analyses variations, and shifting amounts of metal within the secondary loop.

When uniform material and operating conditions are used in combination with schedules insensitive to minor variations, spot welds in steels having a coefficient of variation less than 5% can readily be produced.

Using statistical methods in the determination of the suitability of the original schedules and knowing the influence of welding variations in the results it is possible to establish rational operational limits within which production may be carried out with assurance that welds of the desired strength and uniformity are being produced.

The use of suitable indicating devices for assuring that operations are being maintained within the known limits assures that satisfactory welds are being produced without the need of time consuming and costly routine inspection testing with its resultant production delays.

It is possible to select welding schedules which are sufficiently insensitive to variations in both welding and material conditions that substantial changes can be effected without influencing either the strength or consistency of the results produced. Such schedules are desirable from the point of view of uniformity of production quality.

References

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2. ASA Z1.1-1941 Guide for Quality Control.
3. Heuschkel, J., Some Metallurgical Aspects of Carbon Steel Spot Welding, *Ibid.*, 26 (10) 560-s to 582-s (1947).

(*Continued from page 465-s*)

sample. These fluorescent rays are reflected through a narrow slit at right angles to the path of the primary X-ray to a crystal monochromator. This crystal is in the center of a circle one-fourth of whose circumference is traversed by the Geiger counter in its round of analysis. The counter is moved by motor in this scanning operation.

When used by a prospector for detecting radioactive substances in nature, the counter indicates their presence by clicks which can be counted accurately. The instrument used in the Kearny laboratory has a counting circuit, like other laboratory counters. In this case, however, the counter has been connected to a standard recording instrument, a self-balancing "potentiometer," which automatically plots the intensities of the various elements on graph paper.

This method of analysis is best adapted to detect and measure elements of "middle" atomic weight. Among these are the elements most often combined with iron and carbon to form the various steels in common use—chromium, manganese, molybdenum and nickel, as well as copper, which is frequently added to a steel composition to lessen corrosion. Also included in this class of alloying elements are some whose use is growing more and more frequent, such as columbium, titanium, tungsten and vanadium.

The usefulness of this new and fast method of steel analysis is readily apparent. It promises great advantages in the laboratory, where many determinations of chemical content are necessary. Still more important will be its usefulness, when fully articulated, in steel operations. Here it is necessary for the operator of a steel-making furnace to know quickly, at various steps of his furnace's heating cycle, whether he is getting the exact steel composition required in his order.

Notch Sensitivity of Mild Steel Plates

The notch sensitivity of mild steel plates as determined by the cleavage-tear test, tensile, bend and Charpy impact tests are compared. Simplified testing procedures are described and desirable properties in steel are discussed

by A. B. Bagsar

Abstract

Notch sensitivity characteristics of several open-hearth, rimmed, semikilled and killed steels of structural and pressure vessel qualities, in the form of plates and representing the thickness range of 1/4 to 2 1/4 in., were evaluated by the use of several types of notched bar tests. The tests used were the cleavage-tear group consisting of tensile-bend, tensile and bend tests, and the impact group consisting of a series of Charpy tests using keyhole and V-notched coupons. The criterion of notch sensitivity used in cleavage-tear tests was the changes in nominal yield and nominal breaking strengths, most of which were determined cinematographically, and that in impact tests was the change in energy of fracturing.

For a given steel plate in as-received, normalized, water quenched or strain-aged conditions, and with fractures transverse to rolling, the transition temperatures determined by all tests of the cleavage-tear group agreed with each other closely for a given condition. For steel plates of 1/2 to 1 in. in thickness, the transition temperatures determined by the use of V-notched Charpy coupons were also found to agree with those determined by the cleavage-tear group of tests, and to be about 100° F. higher than those determined by the use of the standard keyhole notched Charpy coupons. No similar agreement was found with fractures parallel to rolling, and with the steel in the annealed condition.

The cleavage-tear tests were found to have certain inherent advantages over impact tests, including the following: Cleavage-tear tests are more informative regarding notch sensitivity characteristics and determine the significant temperature, T_s, at which the yield and breaking strengths of a steel coincide with each other; they disclose anisotropic properties of steel plates; they evaluate the effect of section thickness; they reveal which heat treatments increase and which ones decrease the resistance of a steel to cleavage crack propagation; and they are simpler and more economical. Although no definite relationship was established between grain size and transition temperature, the killed steels as a group were found to be superior to the other grades of the same thickness.

On the basis of the test data presented, simplified testing procedures are described for establishing notch sensitivity characteristics, which are of importance for evaluating the quality of steel plates intended for all-welded construction of monolithic type. Properties which are desirable in steels intended for such applications are discussed.

INTRODUCTION

THE response of different steels to stress is essentially the same within the elastic range, since the modulus of elasticity of all steels is considered to be practically the same. Within the elastic range the strain is simply proportional to stress. However, this simple relationship is not obtained within the plastic range. Different steels manifest different characteristics as the elastic range is exceeded and exhibit individualities with respect to flow, manner of distributing stresses, extent of work hardening and other details. Although the conventional tensile test yields data of practical importance, it does not reveal some of the important individualities of steels within the plastic range. It has long been recognized that the introduction of a suitable notch in the test piece is necessary in order to distinguish the so-called tough or ductile from brittle steels, also to determine the behaviors of steels within the elastic and plastic ranges in presence of a notch.

A notch of suitable acuity restricts lateral flow, induces a state of multiaxial stresses and localizes stresses on a relatively small section of the metal at the root of the notch. Nevertheless, even in notched sections some steels under certain conditions are capable of undergoing considerable flow or deformation at the root of the notch as stress is applied, by which process the localization of stress at that point is materially reduced. As the stress is increased further, additional readjustments occur until a stress level is finally reached at which failure occurs by the ductile or shear mode. Some other steels, on the other hand, are incapable of sustaining any pronounced flow at the base of the notch under the same conditions, as a result of which the section fractures suddenly by cleavage or in a brittle manner as soon as a certain stress is reached. The first type of steels is less sensitive to the notch effect and more ductile than the second type of steels. In the absence of a notch of proper geometry, the difference between the behavior of these two types of steel would not in general be manifest.

In addition to imparting testing sensitivity, it is believed that notches simulate some of the conditions under which cleavage failures occur in service, since it

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Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

has been observed that practically all cleavage cracks in service in monolithic or massive structures originate from a discontinuity or defect acting as the notch. The rise in the yield strength of steel observed on decrease of temperature is more abrupt in the presence of the notch. The practical importance of the latter observation is that if the yield strength rises abruptly within a relatively narrow range of temperature, it will coincide with the breaking strength of the steel at some point within that temperature range and the section will fail by cleavage or in the brittle manner.

In recognition of these facts a large variety of notched bar test coupons have been proposed in the past and used for investigating notch sensitivity. Although several types of impact tests have found favor for the past several decades, it has been felt that the data obtained by these tests are only qualitative, since the test coupons used in impact testing are small in cross section and do not, therefore, simulate the rigidity and multiplicity of stresses encountered in large structures of all-welded construction. In view of this, and because of a number of failures in welded ships, pressure vessels and storage tanks, intensive investigations and research have been undertaken in this field by various agencies and laboratories, with the object of studying the details and variables affecting notch sensitivity determination of steels.

The effect of notches on the behavior of a steel, or briefly the "notch sensitivity" of a steel, can be considered in a broad sense as being an index of the individuality of the steel under the state of stress developed by the geometries of the notch and the test piece, and by the test conditions. In a previous paper[1] the author has reported the results of an investigation on the effects of geometrical variations of the test coupon and of the notch on the fracturing characteristics and ductility transitions of numerous steels, and has indicated that the notch sensitivity of a steel can only be expressed in relative terms of test coupon geometry or of the stress system involved. It has generally been assumed in the past that the notch sensitivity of a steel is best determined by the degree of decrease in its ductility in the presence of a notch. In the paper referred to above, it was shown that notch sensitivity can also be effectively determined by the yield and breaking loads with the use of test coupons of proper geometries, since within the temperature range where pronounced decrease occurs in the ductility, a similar decrease also occurs in the breaking loads.

In the present investigation, two groups of notched bar tests were conducted on nine steels of pressure vessel and ship-plate qualities, covering the thickness range of $^1/_4$ to $2^1/_4$ in. The first group of tests involves a relatively slow speed of testing and is termed "cleavage-tear" because this term describes the manner of fracture propagation in the brittle and ductile states. This group includes tensile-bend or tear, tensile and bend tests. The second group consists of impact tests, using Charpy impact coupons with notches of various types. In the cleavage-tear group of tests notch sensitivity characteristics, including transition temperatures and the temperatures of coincidence of yield and breaking strengths, were established by the nominal yield and breaking stresses determined over a range of temperatures, whereas in the impact group of tests energy of fracturing was used in the conventional manner as the measure of notch sensitivity.

MATERIALS OF TEST

Nine different open-hearth steels in the form of plates

Table 1—Chemical and Physical Properties of Steels Tested

	Steel 15	*Steel 16*	*Steel 17*	*Steel 18*	*Steel 19*	*Steel 20*	*Steel 21*	*Steel 22*	*Steel 23*
Plate thickness, in.	1	1	1	1	1	$2^1/_4$	$^1/_4$	$^1/_2$	$^{15}/_{16}$
Chemical composition, %:									
Carbon	0.20	0.17	0.19	0.20	0.17	0.18	0.21	0.20	0.25
Manganese	0.45	0.40	0.42	0.63	0.44	0.44	0.43	0.45	0.45
Phosphorus	0.019	0.012	0.015	0.020	0.014	0.014	0.010	0.020	0.015
Sulphur	0.029	0.034	0.030	0.033	0.023	0.026	0.028	0.045	0.024
Silicon	0.07	0.18	0.07	0.07	0.17	0.20	0.02	0.01	0.05
Tensile properties (fractures transverse to rolling):									
Tensile strength, psi.	59,300	64,500	58,000	61,300	65,700	62,000	65,800	60,500	63,500
Yield point, psi.	30,400	39,300	32,900	31,400	36,500	39,000	44,100	37,000	33,500
Elongation in 8 in., %	31.0	28.0	31.0	34.4	32.3	33.0	28.0	27.0	31.7
Brinell hardness number	132	143	135	137	142	137	144	160	155
Grain size, normality, and inclusion content:									
Actual grain size	5–6	8–9	6	5–7	6–7	4–5	9	8–9	5–7
Austenitic grain size	3	8.5	3	3–4	6	5–6	[a]	5–6	6–7
McQuaid-Ehn normality	Normal	Slightly abnormal	Normal	Normal	Normal	Normal	[a]	Normal	Normal
Inclusion content and type	D thin 4 and D heavy 3	D thin 4 and D heavy 4	D thin 5 and A heavy 3	D thin 3 and A heavy 1	D heavy 2 and A heavy 1	D heavy 5 and C thin 4	D thin 4 and C heavy 3	D thin 4 and A heavy 2	D heavy 2 and C thin 2

[a] Not determined.

covering the thickness range of 1/4 to 2 1/4 in., were used in this investigation, the chemical and physical properties of which are given in Table 1. In Table 1 are also included for reference the Brinell hardness, the actual and austenitic grain sizes, McQuaid-Ehn normality, and the inclusion type and content of each steel. Steels 16, 19 and 20 were of killed type, Steel 16 being titanium deoxidized and made by the fine grain melting practice. Steels 21 and 22 were of rimmed type, and the remaining steels of semikilled type. As is evident from the data given in Table 1, all the steels selected for this investigation show properties which are normal for the respective grades, as determined by conventional tests.

The microstructures of these steels are shown in Fig. 2, at a magnification of 100 diameters.

Steel 16 was included in order to determine whether or not a fine grained steel of this type would show radically different results than the other steels. The remaining steels were randomly selected from our stock of plates of pressure vessel and ship-plate qualities available at the time, which had been purchased on standard specifications covering these grades; they were not specially purchased for this investigation.

Due to the fact that in some of the steels selected for this investigation there was only a limited stock remaining from previous tests, such steels were subject to a group of representative tests only and not to all the tests described. It is believed that these representative tests are sufficient to indicate clearly the trend in the latter steels, with respect to notch sensitivity.

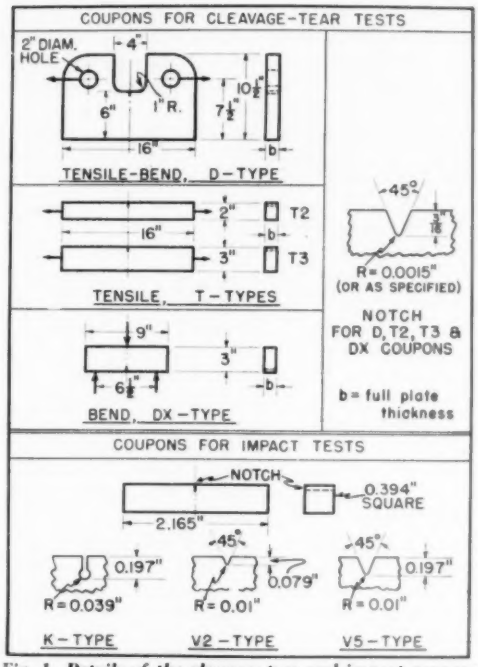

Fig. 1 Details of the cleavage-tear and impact coupons used. Types K and V2 impact test coupons are identical with A.S.T.M. types B and A (A.S.T.M. Specification E23-41T) coupons, respectively

TEST COUPONS, EQUIPMENT AND TESTING PROCEDURES

Test Coupons

In this investigation two groups of tests were used namely, cleavage-tear and impact. The cleavage-tear group consisted of tensile-bend, tensile and bend tests, using a different type of test coupon for each type of test. In the impact group three series of tests were conducted, using the standard Charpy coupon, 10 mm. square by 55 mm. long, but a different type of notch for each series. The dimensional details of these test coupons are given in Fig. 1. The following is a list of the types of tests and coupons used in each of these two groups of tests:

1. Cleavage-Tear Tests:
   - Tensile-bend, D-type coupon.
   - Tensile, T2 and T3 types of coupon.
   - Bend, DX-type coupon.
2. Impact Tests:
   - Standard Charpy, keyhole-notch, K-type coupon.
   - Standard Charpy, 2-mm. deep V-notch, V2-type coupon.
   - Special Charpy, 5-mm. deep V-notch, V5-type coupon.

Of the cleavage-tear group of tests, the tensile-bend test was used extensively by the author in a previous investigation.[1] Use also has been made of the bend[2] and tensile tests by the author in previous investigations, as exploratory tests. In impact tests, the K and V2 types of coupon used are the same as types "B" and "A" test coupons of A.S.T.M. Specification E23-41T, respectively. The V5-type impact coupon is the same as the V2-type except its notch has a depth of 5 mm. instead of 2 mm. as in V2-type. The V5-type coupon was initially used for determining the effect of notch root radius variations on transition temperatures, for which work the use of a deeper notch was found necessary in order to cover a wider range of notch root radius variations without changing the notch depth. However, the V5-type coupon was used considerably in this investigation for other determinations, since V2 and V5 types of coupon were found to yield practically the same transition temperatures.

As shown in Fig. 1, in all test coupons the notch used is located in one edge of the test piece. The use of edge-notched test coupons was given preference because previous work has indicated that test coupons of this type have a satisfactory sensitivity, especially in cases where eccentric loading is introduced.[2, 3]

The blanks for types D and DX coupons were prepared by torch cutting. The edge which was to be notched was machined off to a depth of at least 1/8 in., so as to remove the heat-affected zone. Types T2 and T3 tensile coupons were prepared in the conventional

manner, by machining the two edges. In all cases the full thickness of the plate to be tested was the thickness of the test coupon. The impact test coupons were machined from the mid-thickness of the plate and finished to final dimensions by grinding. They were notched at right angles to the rolled surfaces of the plate.

The V notches in impact coupons were rough machined and then milled to final dimensions, in which operation a single-point milling tool was used. The finished notches were carefully checked for angle and root radius. The keyhole notches in the impact coupons were the conventional type, the holes being drilled with No. 47 drill (radius = 0.039 in.) and then the section saw cut to the drilled hole. In cleavage-tear coupons the notching was done by the pressing method[1] except in those in the normalized and water-quenched conditions. Because of their hardness, the latter coupons were notched by machining, using the two-stage milling method described for notching the impact coupons.

Testing Equipment and Procedures

All cleavage-tear tests were made on a 300,000-lb. capacity hydraulic testing machine. In the case of tensile-bend or D-type coupons, the load was applied through pins inserted in the loading holes of the test coupon, special clevis and pin-type grips being used. For tensile or T-type coupons the conventional grips for testing flat tensile coupons were used. The bend or DX-type coupons were tested edgewise, in the compression section of the tensile testing machine, as a simple beam, they being supported near the ends and the load applied at the center opposite the notched edge, using rounded bars for the supports and for applying the load.

All impact tests were made on a pendulum-type impact testing machine of 240 ft.-lb. capacity, the hammer setting being at the 120 ft.-lb. level and the striking velocity 12.8 ft/sec.

The temperatures of the test coupons were regulated by liquid baths held at the prescribed temperature, immersion being for a duration of about 1 hr. before testing.

In evaluating the notch sensitivity characteristics of a steel, the procedure followed in cleavage-tear tests consisted of determining the nominal yield and nominal breaking strengths at various temperatures. By plotting these strength values against temperature, two important points on the resultant curves were established as indexes of notch sensitivity. One of these was the

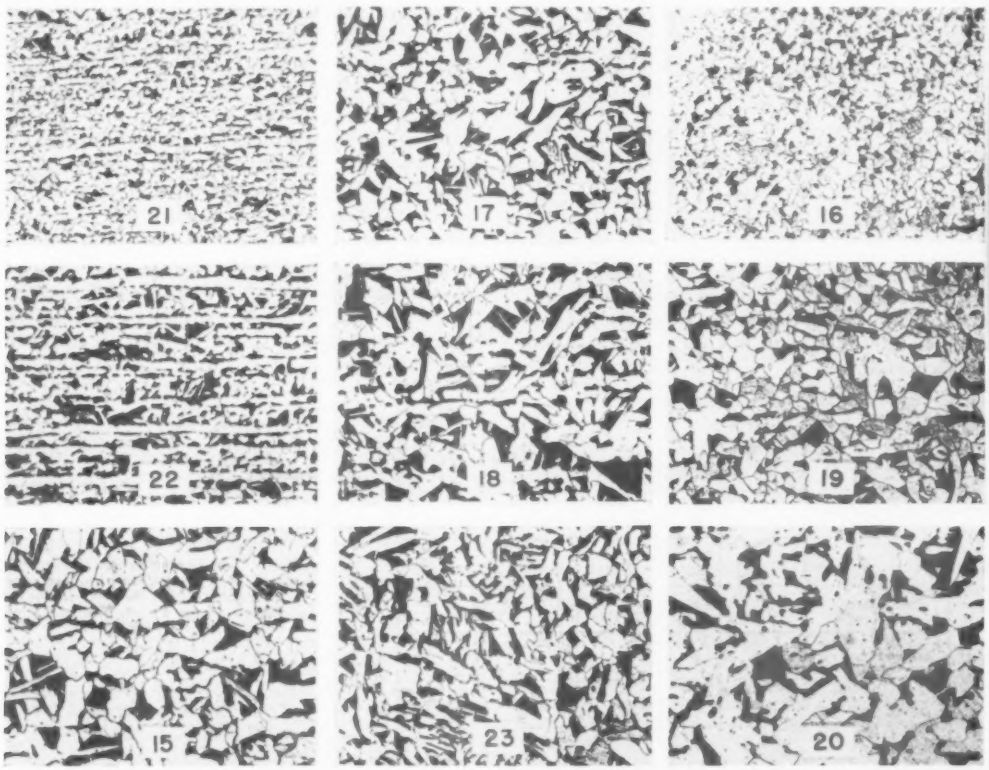

Fig. 2 Microstructure of steels tested, at a magnification of 100 diameters. Steels 21 and 22 are of rimmed, Steels 15, 17, 18 and 23 are of semikilled, and Steels 16, 19 and 20 are of killed quality

Table 2—Cinematographic Determination of Nominal Yield, Y_N, and Nominal Breaking, S_N, Strengths of Steel 23 in Conditions Indicated, by Use of Types D, T3 and DX Coupons (Values Are in Pounds per Square Inch)

D-type coupon; steel, as-received			*T3-type coupon; steel, as-received*			*DX-type coupon; steel, water quenched*		
Time interval, sec.	*Test temp., ° F.*		*Time interval sec.*	*Test temp., ° F.*		*Time interval, sec.*	*Test temp., ° F.*	
	−100	*80*		*−70*	*100*		*−70*	*70*
1/2	21,000	11,200	1/2	10,000	12,800	1/2	34,600	26,600
1	24,800	16,800	1	11,700	14,700	1	50,800	43,900
1 1/2	29,300	22,000	2	16,000	18,300	1 1/2	71,600	61,200
2	33,500	26,500	3	20,300	21,800	2	86,600	78,000[a]
2 1/2	37,300	30,600[b]	4	24,900	24,900	2 1/2	104,000[c]	91,200
3	41,200	34,500	5	29,700	27,400	3	...	102,200
3 1/2	45,400	37,000	6	33,600	29,700	3 1/2	...	110,800
4	48,500	39,100	7	36,500	32,000[b]	4	...	116,600
4 1/2	52,000	42,200	8	38,600	34,000	4 1/2	...	123,500
5	55,000[c]	44,300	9	40,700	34,100	5	...	129,400
5 1/2	...	45,900	10	43,000	35,500	5 1/2	...	134,000
6	...	47,500	11	44,800	37,300	6	...	138,000
6 1/2	...	48,500	11 1/2	45,900[c]	38,200	6 1/2	...	142,000
7	...	49,200	12	...	39,100	7	...	144,500[d]
7 1/2	...	49,500[e]	13	...	40,700	7 1/2	...	148,500
			14	...	42,000	8	...	150,700
			15	...	43,400	8 1/2	...	152,500
			16	...	44,800	9	...	152,500
			17	...	46,500[e]	9 1/2	...	152,500
			18	...	47,500	10	...	150,700
			19	...	48,700	10 1/2	...	149,000
			20	...	50,000	11	...	146,100
			21	...	51,200	11 1/2	...	142,600
			22	...	52,200			

[a] Nominal yield point = 83,200 psi. [b] Nominal yield point. [c] Cleavage fracture (nominal yield strength = nominal breaking strength). [d] Beginning of shear fracture. [e] Cleavage-shear fracture.

transition temperature T_0, which is characterized by the abrupt change in the nominal breaking strength values. The second was the T_s temperature, at which the nominal yield and nominal breaking strengths coincide with each other. In impact tests, the notch sensitivity was determined by the conventional method, consisting of plotting the impact values against temperature. The latter tests established transition temperatures only.

In the cleavage-tear tests the nominal breaking loads and in some cases the nominal yield loads could be determined by direct readings of the load indicating dial of the tensile-testing machine. In most cases, however, the yield loads could not be accurately determined by such direct readings, especially in tests made at temperatures appreciably below the transition temperatures. In all such cases use was made of the cinematographic method.

The cinematographic method simply consists in photographing simultaneously the dial of a running clock and the load-indicating dial of the tensile machine by a motion-picture camera, as the test piece is being loaded to the yield and fracture loads. This establishes the response of the entire test system to load, in terms of time. From the load values thus determined the corresponding nominal stresses for each type of test were computed as described in a subsequent section of this paper. It is apparent that the load or nominal stress, up to the yield point, is directly proportional to time, just as it would be proportional to strain, unless slippage in the grips or other irregularities occur.[1] Typical nominal stress vs. time curves are shown in Fig. 3, which were constructed from the data given in Table 2, derived from cinematographic tests.

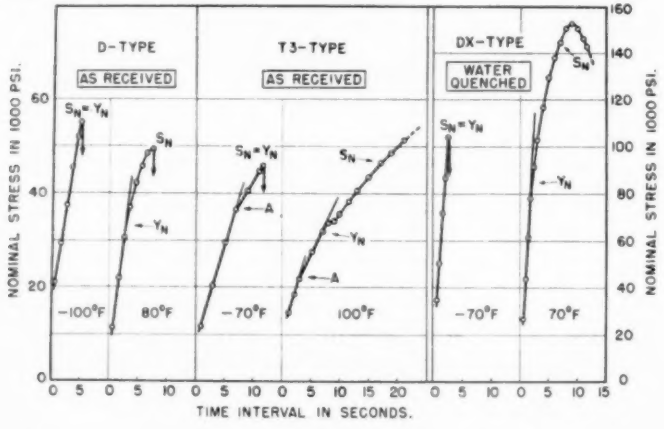

Fig. 3 Nominal stress vs. time curves constructed from data obtained cinematographically. S_N = Nominal breaking stress Y_N = Nominal yield stress

In order to illustrate the usefulness of nominal stress vs. time relationships obtained by the cinematographic method, the details of Fig. 3 will be discussed here. In this figure are included nominal stress vs. time curves obtained on Steel 23, at two temperatures each for tensile-bend, tensile and bend tests. The tests with types D and T coupons were made on the steel in the as-received condition, whereas those made with DX-type coupons were on the steel in the water-quenched condition. It is to be observed that up to the yield point the nominal stress vs. time relationships are represented by straight lines in all these three types of tests, except that a change occurs in the slope of curves of T3-type tests, at points A, probably due to the preliminary yielding or adjustment of the unnotched edge of the test coupon.[4]

Figure 3 shows that at low temperatures the nominal yield stress coincides with or exceeds the nominal breaking stress in all these three types of test, since at low temperatures fractures produce no measurable permanent deformation of the piece as a whole. At high temperatures, the stress vs. time relationship likewise remains a straight line up to the yield point, which point incidentally occurs at a considerably lower level than in the previous case, and pronounced yielding or deformation occurs as the stresses are increased, until fracture develops. The latter is typical of the ductile or shear fracture. At certain intermediate temperatures, such as at the transition temperature range, both ductile and brittle fractures may occur. It is also to be noted from Fig 3. that the water-quenching treatment increases the strength and materially lowers the transition temperature of the steel. This effect is discussed in a later section of the paper, dealing with heat treatments.

Standard Conditions of Test

In this investigation, unless otherwise stated, the steels were tested in the as-received condition and with fractures transverse to rolling of the plate. In all cleavage-tear tests the rate of load application corresponded to a movement of 2 in. per minute of the crossheads of the tensile-testing machine. The standard notch in all cleavage-tear tests was $^3/_{16}$ in. deep with a root radius of 0.0015 in. and 45° included angle. In all cases where the steel was subjected to heat treatments, cold working, aging, etc., the notching of the test coupons was done after the completion of these treatments. For determining certain parameters, deviations have been made in some tests from these standard conditions, which deviations are described in detail in the discussions of the respective tests.

In cases where heat treatments were applied the following standard procedures were used: annealing consisted of heating to 1650° F. for 1 hr. and furnace cooling; normalizing, heating to 1650° F. for 1 hr. and cooling in air; water quenching, heating to 1650° F. for 1 hr. followed by quenching in water from this temperature. Cold working used in these tests comprised proper correction. Expressing this mathematically one stretching the steel at room temperature to obtain 5% elongation in the direction of rolling of the plate. Aging consisted of heating for one hour at 400° F.

TEST RESULTS

Basis for Comparison

It was found necessary to use a common basis for comparison of the results obtained by the various tests, in order to ascertain whether or not a correlation of these results would be possible. For comparing the impact group of test results with those of the cleavage-tear group, the only basis of comparison found was transition temperature, since determination of transition temperature was the only feature which was common to these two groups of tests. For comparing with each other the various types of tests in the cleavage-tear group, the basis of comparison used was the transition temperature, T_0, and the temperature of coincidence, T_n, of the nominal breaking and nominal yield stresses, as revealed by each of these types of tests. As stated previously, T_0 and T_n are established in these tests by the relationship of temperature vs. nominal yield and breaking loads, or temperature vs. the corresponding nominal stresses. Inasmuch as the yield and breaking loads varied greatly depending upon the type of test coupon used, it was felt preferable to express the results in terms of nominal stresses.

In computing the nominal stresses of the cleavage-tear tests from the corresponding values of the load at the yield and breaking points, the presence of the notch in the test coupon was disregarded. For the notched tensile coupons, designated types T2 and T3, these stresses were computed in the customary manner by dividing the total sustained load, P, by the cross-sectional area, A. In the bend tests, in which DX-type coupons were used, the nominal stresses were assumed to be equal to stresses computed by the flexure formula: $S_N = Mc/I$, in which S_N is the nominal stress. M is the bending moment, c is distance of the outer fibers from the centroidal axis of the coupon, and I is the moment of inertia of the section. Although it was realized that this formula is applicable only within the elastic range and in absence of the notch, nevertheless its use was found to be satisfactory for purpose of comparison. It will be noticed from the respective data presented in a subsequent section of this paper that the nominal yield and breaking stresses for bend tests computed by this formula are somewhat higher than those revealed by the other two types of tests in the cleavage-tear group.

In the D-type or tensile-bend tests the nominal stresses are the result of the flexural and the applied tensile stresses. These nominal stresses were computed by the superpositioning method,[1] which consists of adding the tensile stress produced by straight tension to the tensile stress induced by bending, after applying the proper correction. Expressing this mathematically one obtains: $S_N = P/A + n(Mc/I)$, in which S_N represents the nominal stress, P,A,M,c and I have the same defini-

tions as those given in the preceding paragraph, and n is the notch correction factor. For D-type coupons this mathematical expression can be simplified[1] to the form: $S_N = 3.925\ (P/A)$. The nominal stresses for tensile-bend tests could therefore be readily computed by this formula, after P/A values were determined by direct measurements.

In constructing the charts included in this paper the average instead of individual test results were plotted. This was done for the sake of making the charts as legible as possible; the individual test results are given in the respective tables. In all cleavage-tear and impact tests the transition temperature is taken to be the point of inflection of the transition curve.

Notch Geometry

As has been stated the standard notch used in all cleavage-tear tests was $^3/_{16}$ in. deep with 0.0015 in. root radius and 45° included angle. Although the selection of a notch of this geometry was based on experimental data previously published,[1] some additional tests on the effect of notch root radius variations were found to be necessary in the present investigation. Since the geometry of the impact coupon is radically different from that of the tensile-bend coupon, similar tests were also found to be necessary in impact tests, so as to determine the influence of variations in the notch root radius on the transition temperatures determined by the latter tests.

The results of the tensile-bend tests, using notches with different root radii, are summarized in Table 3. A portion of these data is based on previous tests[1] in which the P/A values were used as the index of comparison. These P/A values were converted into nominal stresses by the method described and included in Table 3 for obtaining a broader base of comparison. It is to be noted that in the tests included in this table the notch root radius variations cover the range of 0.0008 to 0.039 in. In order to establish the general trend more definitely, three different steels were subjected to these tests, these being Steels 18, 22 and 23. At the bottom of the table are given the transition temperatures of each steel corresponding to each notch root radius used.

The impact test results on the effect of notch root radius variations are given in Table 7. These tests were made on Steels 17 and 23, using V5-type impact coupons. In this case the variation of the notch root radius covers the range 0.0008 to 0.085 in. The transition temperatures of each steel corresponding to each notch root radius used are similarly given at the bottom of the table.

Figure 4 shows graphically the results of tensile-bend and impact tests on Steel 23 with the use of D-type and V5-type coupons, respectively, containing notches of various root radii. The transition temperatures are indicated by short vertical lines through the curves. It is to be observed from the impact curves given in the upper portion of this figure that the energy vs. temperature curves shift to the left, which means that the transition temperatures decrease, as the notch root radius is increased from 0.01 to 0.085 in. It is also to be observed that the energy vs. temperature curve, as well as the transition temperature, is the same in impact tests for notch root radii 0.0008 to 0.01 in. It appears that impact coupons being small in cross-sectional area lack sufficient rigidity below the notch to be sensitive to variations in the notch root radius within the latter range.

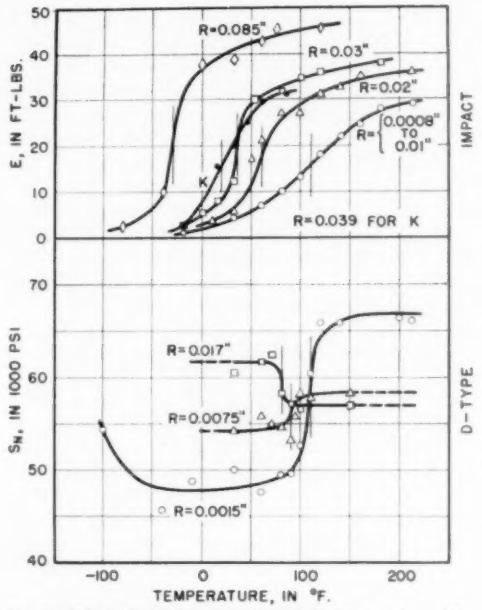

Fig. 4 Effect of notch root radius variations on transition temperature of Steel 23, determined by V5-type impact and D-type tensile-bend tests

An examination of the nominal stress vs. temperature curves for tensile-bend tests shown in the lower portion of Fig. 4 reveals that the transition temperatures established by the tensile-bend test decrease progressively as the notch root radius is increased from 0.0015 to 0.017 in., which was not the case in impact tests just discussed. A second point to be noted is that in this test the nominal breaking stresses for D-type coupons containing notches with root radii from 0.0015 in. up to 0.0075 in. are lower for temperatures immediately below the transition temperatures than those above the transition temperatures, whereas for coupons having a notch with 0.017 in. root radius the reverse is true. It is apparent that in tensile-bend tests the use of notches greater than about 0.01 in. in root radius modifies the stress system in such a manner as to make the nominal stress for cleavage considerably higher than that for shear. The third point to be observed is that the transition temperature for Steel 23 as indicated by V5-type impact coupons with notches 0.0008 to 0.01 in. in root radius is practically the same as that indicated by D-type tensile-bend coupon containing the standard notch with 0.0015 in. root radius.

In Fig. 5 is shown a somewhat broader comparison between the impact and tensile-bend test results. In this figure the transition temperatures, in ° F. absolute, of several steels are plotted against notch root radii, using logarithmic scale for both axes of the plot. The left half of this figure shows the relationships of the transition temperature, T_0, vs. notch root radius R, for Steels 18, 22 and 23, determined by tensile-bend tests with the use of D-type coupons. The right half of Fig. 5 shows the same relationships of T_0 vs. R for Steels 17 and 23,

Table 3—Effect of Temperature on Nominal Yield Strength, Y_N, and Nominal Breaking Strength, S_N, in Psi., Determined by Use of D-Type Coupons with Indicated Notch Root Radius, R

Test temp., ° F.	R = 0.0008 in.		R = 0.0015 in.							
	Steel 18, S_N	Steel 22, S_N	Steel 15, S_N	Steel 18		Steel 21		Steel 22	
				Y_N	S_N	Y_N	S_N	Y_N	S_N
−100	.....	.....	.....	57,200	57,200	64,000	64,400[a]	...	.....
−70	.....	.....	.....	...	.....	...	61,500[a]	...	.....
−55	.....	.....	.....	...	.....	57,400	59,000[a]	...	.....
−40	.....	.....	.....	47,100	47,100	...	.....	...	.....
−25	.....	.....	.....	...	.....	53,700	55,000[a]	...	.....
−10	.....	.....	49,100[b]	...	.....	...	57,000[a]	...	.....
−5	.....	.....	.....	...	.....	...	58,600[c]	...	.....
0	.....	.....	.....	...	.....	...	59,000[c,d]	...	.....
20	.....	.....	.....	...	.....	53,000	58,000[c,d]	52,200	52,200
32	44,600–45,500	48,300–51,000	47,500–48,700[b]	47,700	47,100–47,500	...	58,900[c,d]	...	49,900–51,000
60	.....	.....	.....	...	.....	...	.....	...	51,000
70	47,100	49,800	49,100[b]	...	46,300–48,300	...	.....	...	50,200–51,400
80	45,100	51,000	.....	36,500	49,500	...	.....	...	.....
90	.....	.....	.....	...	46,300–52,200	...	53,000[c,d]	...	51,800
95	.....	.....	48,300–49,100[b]	39,000	53,000	...	.....	...	.....
100	45,900–47,500	53,000	56,200	...	48,300–64,800	...	.....	52,200	58,800–60,000
105	.....	.....	62,400[b]	...	49,100–56,200	...	.....	...	.....
110	.....	58,800–59,600	64,000–66,700[b]	38,500	58,900	...	.....	51,800	.....
115	.....	.....	.....	...	60,800–65,600	...	.....	...	.....
120	47,100–62,000	57,000–58,800	.....	37,300	.....	...	.....	52,200	61,200
125	.....	.....	65,600	...	.....	...	.....	...	.....
130	52,200–61,200	58,800–59,300	.....	...	64,400	...	.....	...	.....
140	58,800–64,000	59,700–62,000	.....	...	.....	...	.....	...	.....
186	.....	59,300	.....	...	67,200	...	.....	49,900	60,000
200	.....	.....	64,000	...	.....	...	.....	...	.....
T_0[e]	125° F.	105° F.	103° F.	...	105° F.	...	−10° F.	...	92° F.

Test temp., ° F.	R = 0.0015 in.			R = 0.0075 in.				
	Steel 23			Steel 18		Steel 22		Steel 23, S_N
	Y_N	S_N	S_N[f]	Y_N	S_N	Y_N	S_N	
−100	53,800–55,000	53,800–55,000	.....	.....	56,200–59,700	...	.....	.....
−70	.....	.....	.....	.....	60,000–60,900	...	.....	.....
−40	45,500	45,500	.....	.....	.....	...	.....	.....
−20	.....	.....	.....	55,000	58,900	...	.....	.....
−10	42,200	48,700	.....	.....	.....	...	.....	.....
32	35,300–36,900	48,300–52,200	43,200–47,900	49,100	59,900	57,000	60,500–60,800	53,800–55,000
50	.....	.....	.....	.....	.....	53,800	60,000–61,600	.....
60	29,800	47,500	.....	.....	.....	53,800	66,800–68,000	55,800
70	.....	.....	.....	46,300	62,400	54,200	65,600–66,000	55,000
80	30,600	49,500	.....	.....	.....	...	.....	52,200–57,700
90	.....	49,500–49,900	45,600–46,700	45,900	69,900	53,800	66,800	51,400–55,000
95	.....	49,900–55,800	.....	.....	.....	...	.....	55,800
100	31,400	49,800–55,700	43,200	43,200	64,800	...	.....	58,100
110	27,500–30,900	53,000–67,500	45,900	.....	.....	...	.....	57,700
120	.....	62,800–69,000	45,200	.....	66,800	...	.....	.....
130	.....	.....	45,200–45,200	.....	.....	...	.....	.....
140	.....	62,800–69,000	45,900–46,300	.....	.....	...	.....	.....
150	.....	.....	45,900–57,700	.....	.....	...	.....	58,300
200	30,500	65,600	45,900	.....	.....	...	.....	.....
212	33,600	66,000	.....	42,000–43,200	65,200–66,000	...	.....	.....
T_0[e]	.....	110° F.	[g]	.....	80° F.	...	58° F.	90° F.

Test temp. ° F.	R = 0.017 in.			R = 0.039 in.		
	Steel 22		Steel 23, S_N	Steel 18, S_N	Steel 22	
	Y_N	S_N			Y_N	S_N
−40	57,700	66,800	.....	.....	60,000	75,800
−10	60,000	66,400–68,700	.....	.....	59,700–60,800	72,600
20	55,000	67,900	.....	74,600	56,200–56,200	72,400
32	55,000–56,200	65,200	60,400	70,700–73,800	51,800–57,300	70,600–73,800
40	52,200–55,000	61,600–65,600	.....	73,800	51,800–52,200	69,500–73,000
50	52,200–55,000	65,600–66,800	.....	.....	52,200–53,800	70,600–70,600
60	.....	.....	61,200–62,000	74,600	.....	.....
70	53,400	63,600–66,800	60,500–64,000	68,700–69,500	51,000–52,200	68,700–69,500
80	.....	.....	58,100–58,100	68,300–69,500	52,200	68,300–68,300
90	.....	.....	57,000	.....	.....	.....
95	.....	.....	.....	69,100	51,000	69,100
100	.....	.....	56,600	.....	.....	.....
150	.....	.....	57,000	.....	.....	.....
212	40,400	62,800	.....	66,700	43,200	66,700
T_0[e]	.....	45° F.	80° F.	50° F.	.....	30° F.

[a] Cleavage fracture. [b] Notch angle, 60°. [c] Shear fracture. [d] Test coupon buckled. [e] T_0 = Transition temperature. [f] Fracture parallel to rolling; in all others, fracture transverse to rolling direction of plate. [g] Indeterminate by S_N vs. T relationship, within the temperature range covered.

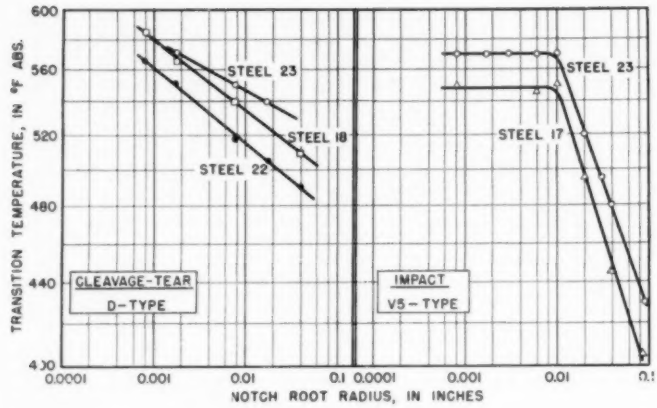

Fig. 5 Relationships between transition temperature and notch root radius, as revealed by V5-type impact and D-type tensile-bend tests made on several steels

determined by impact tests using V5-type coupons.

It is evident from Fig. 5 that for a given steel impact tests yield the same transition temperature with the use of notches of any root radii within the range of 0.0008 to 0.01 in., which was shown to be the case by Fig. 4. It is also evident from Fig. 5 that notches greater than 0.01 in. and up to 0.085 in. in root radii show progressively lower transition temperatures in impact tests, the lowest transition temperature being revealed by the notch with the largest root radius. It is to be observed from this figure that in tensile-bend tests the situation is different. In the latter case the relationship between the notch root radii R and the corresponding transition temperatures is a straight line throughout the entire range of R covered, the transition temperatures decreasing as the notch root radius increases.

On the basis of these data, impact coupons containing notches of any root radius in the range 0.0008–0.01 in. could be used without affecting transition temperature determinations. For this reason, and for reason of ease of machining, the standard notch root radius for types V2 and V5 impact coupons used in all comparative tests of this investigation was 0.01 in. In this connection it is to be noted that the scatter in the test results generally found in V-notched coupons could be materially reduced if the notch root radius for these coupons were specified to be, say, 0.01 + 0 − 0.005 in. instead of the present value of 0.01 in. As has been explained, this minus tolerance does not affect transition temperature determinations or impact values.

Cleavage-Tear and Impact Tests

The steels selected for this investigation were subjected to cleavage-tear and impact tests, using the various types of test coupons and testing procedures previously described. These tests were conducted at various temperatures for determining the transition temperatures and other characteristics of the steels. The results of the cleavage-tear tests are summarized in Tables 3 to 6, and those of the impact tests in Tables 8 and 9. These data are graphically presented in Figs. 6 to 10. In Table 7 are given results of tests on notch root radius variations in impact coupons, which has already been discussed.

In Table 3 are reported the results of tensile-bend

Table 4—Effect of Temperature on Nominal Breaking Strength S_N, in Psi. of Steels 16, 17, 19 and 20, Determined by the Use of D-Type Coupons with Notch Root Radius, R = 0.0015 In.

Test temp., ° F.	*Steel 16, S_N*	*Steel 17, S_N*	*Steel 19, S_N*	*Steel 20, S_N*
−25	55,700	...	...	...
0	55,700–59,600	...	...	...
32	57,600	47,900	58,850–62,000	45,150–47,100
55	...	...	61,200	...
60	...	...	55,000–68,650	...
62	59,700	...	...	...
70	66,600	...	71,800–72,600	...
74	...	47,500	...	...
75	68,700	...	...	...
79	...	48,300	...	...
80	...	...	69,450	47,100
87	...	61,600	...	...
90	70,600	...	69,450	...
100	...	62,800	...	47,500–48,300
120	...	...	...	51,800–62,800
140	...	...	...	66,700
150	...	...	...	68,300
212	...	...	...	70,200
T_0*	68° F.	83° F.	65° F.	120° F.

* T_0 = Transition temperature.

Table 5—Effect of Temperature on Nominal Yield Strength, Y_N, and Nominal Breaking Strength, S_N, in Psi. Determined by Use of T-Type Test Coupons with Notch Root Radius, R = 0.0015 In.

Test temp., ° F.	Steel 15		Steel 17,	Steel 18		Steel 21		Steel 23	
	Y_N	S_N	S_N[a]	Y_N	S_N	Y_N	S_N	Y_N	S_N
−70	45,000	45,000	.....	44,500	44,500	52,000	55,300	45,000–45,900	45,100–45,900
−40	.....	.....	42,000–43,500	...	...	48,000	51,300	31,800–35,200	40,800–42,000
−20	.....	.....	.....	...	...	44,000	48,000	.....	.....
−10	.....	.....	.....	...	...	46,000	51,300	.....	.....
0	30,700–31,700	35,000–36,000	39,500	37,000	37,300	44,600	51,000	32,500–33,800	38,600–39,800
32	29,700	34,900	37,000	36,000	38,000	...	...	29,200–33,000	35,500–36,500
50	.....	.....	35,000–39,000	...	...	42,700	49,900	.....	.....
70	.....	.....	37,000–45,000	33,300	38,000	...	...	.....	.....
80	.....	.....	36,000–49,800	...	...	...	...	30,000–31,000	38,200–38,600
90	30,000–31,000	33,000–41,300	43,000–50,500	31,600	38,300	42,200	48,000	.....	40,000
100	.....	.....	.....	32,300	38,300	...	...	31,000–32,000	43,000–46,500
110	.....	.....	50,000–51,200	32,000	44,000	...	...	.....	.....
120	30,700–31,300	40,000–43,000	.....	31,700	48,300	...	...	31,800	45,600
140	30,000–30,600	43,000–45,000	.....	29,300	47,000	...	...	28,000–31,000	43,000–53,500
150	.....	.....	49,700–50,000	...	...	...	...	.....	.....
160	30,000	45,500	.....	...	...	...	...	.....	.....
180	.....	.....	48,800–50,000	...	...	...	...	30,200	54,300
212	29,000–30,000	47,000–48,300	.....	30,000	43,000	...	...	30,000	54,200
T_0[b]	.....	115° F.	80° F.	...	105° F.	...	−10° F.	.....	110° F.

[a] T2-type test coupon; all others, T3-type. [b] T_0 = Transition temperature.

Table 6—Effect of Testing Temperature on Nominal Yield Strength, Y_N, and Nominal Breaking Strength, S_N, of Steels 18 and 23, in the Conditions Indicated, by the Use of DX-Type Test Coupons, with Notch Root Radius, R = 0.0015 In

Test temp., ° F.	Steel 18, as-received		Steel 23, as-received				
	Y_N[a]	S_N[a]	Y_N[b]	S_N[b]	Y_N[a]	S_N[a]	S_N[a,c]
−100	71,400	67,200–71,400	...	...	104,000	110,300–111,000	.....
−70	.....	.....	64,500	69,300	64,500	64,500–67,000	60,000
−40	54,200–54,600	69,300–73,700	48,500	65,800	46,200–64,500	67,000–72,800	.....
−20	.....	.....	...	.....	.....	69,300–71,500	.....
0	49,700–60,600	68,200–68,200	39,500	68,100	37,000–43,900	68,100–71,500	.....
32	54,200–54,200	62,800–71,400	...	67,000–71,600	.....	65,800–69,300	61,200–64,500
50	.....	69,300–70,400	...	.....	.....	.....	.....
60	52,000–54,200	65,100–71,400	...	.....	40,500	65,800–68,100	.....
80	34,700–35,800	67,200–72,600	...	.....	38,100	64,500	.....
90	.....	69,300–70,400	...	77,400–85,200	.....	65,800–65,800	63,500–64,700
100	.....	72,600–79,100	...	80,400–95,800	37,000	64,700–68,100	61,200–67,000
110	.....	109,500–111,500	...	.....	.....	68,100–69,300	.....
120	.....	108,300–110,500	...	73,900–99,300	.....	72,800–78,600	.....
130	36,800–40,100	110,500–111,500	...	.....	.....	80,900–98,200	63,500–69,300
140	.....	110,500–112,600	34,600	102,700–104,000	34,600–35,800	86,600–92,400	.....
150	.....	108,300–110,500	...	.....	.....	76,600–109,800	61,200–99,500
160	.....	.....	...	.....	.....	.....	65,800–68,100
180	.....	.....	...	.....	.....	.....	76,200–82,000
212	37,900–40,000	103,000–108,300	37,000	102,700	34,600	93,600–104,000	81,000–82,000
T_0[d]	.....	105° F.	...	110° F.	.....	120° F.	170° F.

Test temp., ° F.	Steel 23 in conditions indicated							
	Annealed, S_N[a]	Normalized, S_N[a]	Water quenched		Water quenched and aged		Cold worked, S_N[a,f]	Cold worked and aged, S_N[a,f,g]
			Y_N[b]	S_N[b]	Y_N[b,e]	S_N[b,e]		
−100	.....	.....	109,800	109,800	104,000	104,000	.....	.....
−70	.....	.....	104,000	104,000	102,800	111,500–126,000	.....	.....
−40	.....	67,000–71,500	...	...	...	...	.....	.....
−20	.....	.....	89,000	112,500	92,400	111,000	.....	.....
0	.....	.....	92,500	119,500	83,200	111,000	.....	.....
32	60,000–63,500	63,500–73,900	83,200	129,300	80,900	108,000–108,600	67,000–72,800	84,300–86,600
50	.....	.....	...	125,000	...	...	.....	.....
60	.....	63,500–68,100	...	112,000	...	141,500	.....	.....
70	.....	.....	83,200	144,500	...	143,300	.....	.....
80	.....	57,700–69,300	...	...	...	143,300	.....	.....
90	60,000–68,100	83,000–92,400	...	...	...	.....	82,000–93,500	86,600–94,800
100	.....	82,000	...	148,000	...	.....	.....	.....
120	.....	87,800	83,200	135,000	68,500	143,300	86,600–94,700	90,000–108,500
130	52,000–63,500	.....	...	...	...	.....	.....	.....
150	63,500–92,400	.....	...	...	...	.....	77,000–109,500	.....
160	54,300–57,700	78,500	...	...	...	.....	90,000–108,500	93,600
170	.....	.....	...	...	...	.....	108,500–103,600	97,000–100,500
180	50,800–63,500	.....	...	...	...	.....	106,000–108,000	113,500
212	63,500–86,600	88,900	75,000	136,300	68,500	126,000–128,500	106,800–108,500	108,500–112,600
T_0[d]	180° F. (?)	85° F.	...	50° F.	...	50° F.	150° F.	170° F.

[a] Pressed notch. [b] Milled notch. [c] Fracture parallel to rolling direction; in all others fracture transverse to rolling. [d] T_0 = Transition temperature. [e] Aged 1 hr. at 400° F. after the treatment indicated. [f] Stretched 5% in rolling direction, at atmospheric temperature, after the treatment indicated. [g] Stretched 5% in rolling direction, at atmospheric temperature, and aged as in *e*.

Table 7—Effect of Notch Root Radius on Impact Properties of Steels 17 and 23 in As-Received Condition, Determined by Use of Charpy Coupons V5-Type with Indicated Notch Root Radii, R[a] (Values Are in Ft.-Lb.)

Test temp., °F.	Steel 17				Steel 23						
	R = 0.0008 in.	R = 0.006 in.	R = 0.02 in.	R = 0.085 in.	R = 0.0008 in.	R = 0.0015 in.	R = 0.003 in.	R = 0.006 in.	R = 0.02 in.	R = 0.03 in.	R = 0.085 in.
−80	...	...	...	4, 9	...	...	...	...	...	...	2, 3
−60	...	...	...	10, 15, 24	...	...	...	...	...	...	...
−50	...	...	...	19, 33	...	...	...	...	...	...	...
−40	...	...	...	23, 25	...	...	...	...	...	...	4, 17, 19
−20	...	...	3, 4	24, 37	...	...	1, 2	1, 2	...	2, 3	...
0	2, 3	2, 3	4, 9	30, 36	...	...	...	...	...	4, 7	37, 39
15	...	...	5, 13	...	...	...	...	...	...	5, 6, 15	...
32	4, 6	5, 6	16, 17	32, 38	4, 4	3, 4	4, 5	3, 5	5, 6	6, 11, 20	14, 39, 39
50	...	9, 12	...	...	...	...	...	...	13, 20	29, 31	...
60	6, 8	7, 12, 16	24, 25	33, 37	6, 6	7, 8	7, 8	8, 9	20, 22	...	42, 43
80	11, 13, 14	16, 16	...	...	9, 10	...	...	...	26, 28	30, 33	...
85	...	...	...	...	...	...	11, 13	10, 13	...	...	45, 46
100	14, 17	20, 21, 21	29, 30	...	13, 13	11, 17	...	...	27, 27	34, 35	...
120	20, 25	27, 29	...	...	16, 18	...	17, 19	19, 22	31, 31	...	43, 46
140	23, 24	31, 32	35, 35	...	20, 21	20, 24	...	...	32, 33	35, 37	...
150	...	...	...	...	...	...	24, 25	23, 27	...	...	...
160	...	...	...	...	24, 25	...	...	...	35, 35	...	...
180	27, 28	30, 31	...	...	27, 29	27, 29	...	...	...	37, 40	...
212	28, 29	30, 31	34, 35	...	30, 30	28, 29	30, 31	28, 32	36, 36	...	...
260	...	...	...	...	27, 28	29, 30	29, 31	31, 32	...	...	47, 48
T_0[b]	90° F.	85° F.	35° F.	−55° F.	110° F.	110° F.	110° F.	108° F.	60° F.	35° F.	−30° F.

[a] Impact values of these steels for R = 0.01 in. (V2 and V5 types of coupons) and for R = 0.039 in. (K-type coupon) are given in Table 8. [b] T_0 = Transition temperature.

Table 8—Impact Properties of Steels 15 to 22, in the As-Received Condition (Values Are in Ft.-Lb.)

I. Standard Charpy with keyhole notch (K-type coupon)

Test temp., °F.	Steel 15	Steel 16	Steel 17	Steel 18	Steel 19	Steel 20	Steel 21[b]	Steel 22
−100	...	...	...	...	...	...	2, 2, 4	...
−80	...	...	...	...	2, 3	...	13, 14, 14	...
−50	...	...	...	...	2, 6, 15	...	16, 18	2, 4
−40	...	...	...	...	2, 11, 20	...	...	...
−35	...	5, 7	5, 6	...	...	...	...	...
−30	...	...	...	...	11, 21, 22	2, 2	17, 18	5, 6
−20	...	23, 25	5, 10, 12	3, 6	...	3, 6	...	11, 14, 19
−10	...	...	18, 22	10, 10	...	...	...	15, 19
0	5, 6	28, 29	...	17, 18, 24	24, 24	5, 8, 18	18, 20	17, 18
15	6, 11, 13	...	23, 26	17, 23, 27	...	27, 28	...	...
25	21, 21	...	...	...	...	...	...	...
32	18, 30	...	26, 28	18, 23	25, 26	29, 31	21, 23	21, 21
45	28, 30	...	...	31, 34	...	...	...	...
60	...	...	...	...	...	33, 35	21, 23	...
85	35, 37	33, 35	31, 35	32, 34	32, 32	38, 39	...	22, 24
140	...	...	...	...	36, 37	38, 40	...	...
T_0[c]	18° F.	−32° F.	−17° F.	5° F.	−38° F.	10° F.	−90° F.	−15° F.

II. V5-type impact coupon, unless otherwise stated

−40	...	2, 2	...	...	3, 6	...	2, 2	...
−20	...	...	...	2, 2	...	3, 6	2, 3	1, 2
0	...	3, 4	2, 2	...	7, 11	...	7, 9	...
10	...	...	...	...	...	8, 12	7, 8	3, 3
20	...	...	...	...	...	...	10, 12	...
32	2, 3	10, 14	4, 5	3, 3	5, 6	9, 26	12, 16	5, 6	11, 12	5, 7
40	...	...	...	...	...	...	...	...	10, 13	...
50	...	17, 18	...	...	...	...	...	...	12, 14	...
60	4, 7, 10	14, 15	18, 22, 27	8, 9	12, 14	35, 35	19, 19	10, 15	16, 17	10, 11
70	5, 6, 16	21, 24	...	...	...	...	...	...	...	...
80	6, 9, 9	...	10, 35, 41	13, 14	16, 18	27, 30	17, 20, 22	13, 14	...	12, 12
85	...	22, 23	...	...	...	...	...	...	...	...
90	6, 7, 9	...	33, 38	...	27, 28	...	...	19, 20	...	...
100	21, 21	...	33, 41	17, 20	20, 22	52, 54	24, 25	23, 30	17, 19	14, 15
120	23,26	23, 25	50, 50	23, 23	20, 21, 27	53, 54	28, 28	29, 31	...	15, 17
140	...	...	50, 62	26, 27	28, 28	56, 65	...	...	...	20, 21
150	...	25, 30	...	...	...	...	...	30, 30	...	...
160	29, 31, 31	...	...	...	30, 31	...	30, 30	...	18, 18	22, 22
180	29, 34	31, 31	72, 73, 76	32, 32	...	...	...	35, 36	...	...
212	34, 35	...	69, 70, 73	30, 32	29, 30	64, 67	29, 30	...	...	21, 22
260	30, 32	...	...	...	...	...	...	34, 35	...	...
T_0[c]	95° F.	55° F.	90° F.	90° F.	90° F.	75° F.	65° F.	95° F.	10° F.	80° F

[a] V2-type impact coupon. [b] Same as V5-type, except coupon was 0.236 in. thick by 0.394 in. wide, with notch (R = 0.01 in.) milled across the thickness. [c] T_0 = Transition temperature.

tests on Steels 15, 18, 21 and 22, in the as-received condition, by the use of D-type coupon containing the standard notch of 0.0015 in. root radius. Included in this table are also results of tensile-bend tests on Steel 23 in the as-received condition, with fractures in two directions: transverse, and parallel to the direction of rolling of the plate. In Table 4 are given the results of tensile-bend tests on Steels 16, 17, 19 and 20, all the steels being in the as-received condition, and in all cases the fractures being transverse to rolling. Some of the data included in the latter table are based on previous tests,[1] but have been included as augmentary data. The tensile test results obtained on Steels 15, 18, 21 and 23, in the as-received condition, are summarized in Table 5, in which tests T3-type tensile coupon was used. In the same table are also given data on Steel 17, obtained on the as-received steel by the use of T2-type tensile coupon. Table 6 summarizes the bend test results on Steels 18 and 23 in the as-received condition, also on Steel 23 in the annealed, normalized, water quenched, water quenched and aged, 5% cold worked, and 5% cold worked and aged conditions, using DX-type coupon.

In Table 8 are tabulated the results of impact tests on Steels 15 to 22, inclusive, by the use of types K and V5 impact coupons, also V2-type impact test results on Steels 17 and 19, all these steels being in the as-received condition and with fractures transverse to rolling.

Table 9 summarizes the impact test results on Steel 23 in the as-received condition by the use of types K, V2 and V5 impact coupons, with fractures parallel, and transverse to rolling. Paralleling the bend tests summarized in Table 6, Steel 23 was also subjected to impacts tests using types K and V5 coupons in the annealed, normalized, water quenched, water quenched and aged, 5% cold worked, and 5% cold worked and aged conditions. The results of these tests are also included in Table 9.

A comparison of the tensile-bend, tensile, bend and impact test results obtained on Steels 18 and 23, through the temperature range of −100° to 212° F. is shown by Fig. 6. The transition temperatures are indicated by short vertical lines through the curves, which lines are not labeled. It is to be observed that the transition temperatures of Steel 18 determined by V5-type impact and the three types of tests of the cleavage-tear group are approximately the same, also that the transition temperatures revealed by K-type impact tests are in round figures about 100° lower. The same situation prevails in case of the transition temperatures of Steel

Table 9—Impact Properties of Steel 23, in Conditions Indicated (Values Are in Ft.-Lb.)

I. Standard Charpy with keyhole notch (K-type coupon)

Test temp., ° F.	*As-received*		*As-received*[b]		*Annealed*	*Normalized*	*Water quenched*	*Water quenched and aged*[c]	*Cold worked*[d]	*Cold worked and aged*[e]
−110	[a]		[a]		...	...	3, 6	8, 8	...	...
−90	...	...	...	...	...	...	11, 20	2, 4	...	...
−75	...	...	...	...	...	...	16, 26	13, 37	...	...
−60	...	...	...	...	...	2, 2	28, 32	27, 30	...	...
−40	...	...	...	1, 2	...	3, 13, 18	29, 31	29, 30	...	...
−20	...	2, 3	...	3, 4	...	4, 23, 25	36, 37	32, 34	...	...
−10	...	5, 5	...	...	...	23, 25	...	...	...	...
0	...	...	...	5, 5	2, 2	28, 29	40, 42	26, 37, 38	...	...
15	...	10, 21	...	...	...	...	...	28, 41, 42	...	...
32	...	18, 23	...	20, 22	6, 7	31, 32	20, 39, 43	...	4, 4	2, 3
45	...	24, 26	...	...	8, 24	...	...	...	...	...
50	...	...	...	...	19, 21	...	...	...	...	...
60	...	29, 30	...	24, 26	21, 26	...	50, 52	...	7, 8	4, 7
75	...	...	...	...	...	...	...	...	8, 19	9, 12
87	...	31, 32	...	...	26, 27	37, 38	49, 51	44, 46	10, 22	12, 18
100	...	...	...	28, 28	...	...	...	...	22, 25	19, 21
120	...	...	...	...	...	...	...	...	23, 23	25, 25
140	...	...	...	30, 30	27, 30	...	...	...	...	27, 28
T_0[f]	...	20° F.	...	20° F.	45° F.	−20° F.	−60° F.	−60° F.	80° F.	88° F.

II. V5-type impact coupon, unless otherwise stated

−20	...	...	...	...	...	2, 2	4, 5	4, 6	...	...
0	...	2, 2	...	2, 2	...	...	9, 11	6, 9, 11	...	...
10	...	...	...	...	...	3, 5	...	...	...	...
32	5, 6	5, 6	6, 6	4, 5	2, 3	8, 9	10, 12	11, 12	2, 2	...
50	...	...	...	...	...	...	...	...	...	3, 3
60	8, 12	8, 8	...	5, 7	4, 4	13, 14	13, 18	...	...	...
80	13, 14	...	11, 13	9, 10	6, 9	18, 20	16, 18	14, 15, 20	...	4, 5
90	17, 17	12, 14	...	...	...	...	...	...	4, 5	...
100	22, 26	...	17, 20	12, 13	11, 13	20, 23	18, 19	11, 14, 24	5, 6	...
120	34, 36	18, 19	23, 25	14, 15	15, 15	24, 26	23, 28	30, 31	8, 9	6, 9
130	...	...	...	...	...	...	...	...	14, 15	...
140	...	...	30, 31	16, 17	18, 20	29, 30	25, 32	18, 25, 30	14, 18	10, 11
150	41, 43	21, 24	...	...	...	...	...	...	13, 19	12, 14
160	...	...	...	17, 18	20, 23	...	...	...	18, 24	20, 26
180	46, 49	22, 24, 25	37, 39	19, 19	24, 25	30, 30	30, 31	21, 29, 29	22, 25	22, 26
212	51, 52	29, 30, 31	43, 43	20, 20	26, 27	32, 32	29, 30	32, 33	23, 28	26, 26
260	63, 66	29, 30	45, 46	...	25, 26	...	...	...	...	30, 30
T_0[f]	115° F.	110° F.	120° F.	95° F.	120° F.	70° F.	60° F.	65° F.	135° F.	160° F.

[a] V2-type impact coupon. [b] Fracture parallel to rolling; all others, fracture transverse to rolling of plate. [c] Aged 1 hr. at 400° F. after the treatment indicated. [d] Stretched 5% in rolling direction. [e] Stretched 5% in rolling direction and aged as in footnote c. [f] T_0 = Transition temperature.

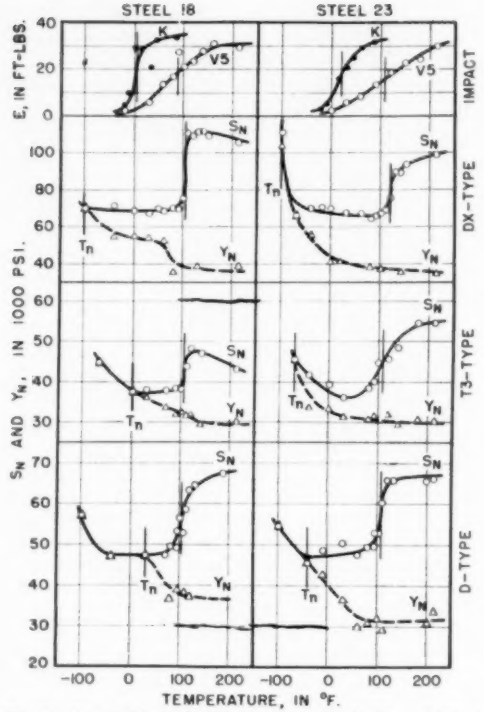

Fig. 6 Comparison of properties of Steels 18 and 23 at various temperatures as revealed by T3-type or tensile, DX-type or bend, and D-type or tensile-bend tests of the cleavage-tear group, and by types K and V5 tests of the impact group

Transition temperatures are indicated by short vertical lines which are not labeled. The T_n temperatures are similarly indicated and labeled

23 determined by these different tests. In this figure are also shown the T_n temperatures of these two steels as revealed by types D, T3 and DX tests, this temperature being indicated by short vertical lines labeled T_n. The T_n temperature is defined[1] as being the highest temperature at which the nominal yield and nominal breaking strengths of a given steel coincide. It is evident from the respective curves that while the transition temperature for a given steel in all these three cleavage-tear tests are approximately the same, the T_n temperatures indicated by these different tests are not the same. For both steels the highest T_n temperature occurs in the tensile-bend test. The multiaxial stress conditions developed in this coupon by the combination of tension and bending, tend to raise T_n to higher temperatures. The intermediate T_n temperature was observed in the tensile test, and the lowest in the bend test. It appears then that the T_n temperature can be varied considerably by the state and mode of application of stress. The T_n temperatures cannot be determined by the impact test, due to the inherent limitations of this test previously described.

A comparison of Steels 15 and 21 under tensile-bend, tensile and impact tests is depicted by Fig. 7. Due to the thickness limitation of Steel 21, subsize coupons were used for the impact tests of this steel, as indicated in Table 8. It is to be noted that the transition temperatures of Steel 15 revealed by the cleavage-tear tests likewise agree with those determined by V5 impact tests, within the experimental accuracy. The same applies to Steel 21, except the transition temperature of this steel is about 20° F. higher for the V5-type test than that shown by the tensile-bend test. A closer check could not be expected since Steel 21 was only $^1/_4$ in. thick. The peculiar shape of the tensile-bend and tensile curves of Steel 21 is due to the fact that this steel, being only $^1/_4$ in. thick, does not have sufficient rigidity to resist bending or buckling when the test temperature exceeds the transition temperature. Nevertheless, below the transition temperature the D-type test coupons of this steel remained rigid and fractured by cleavage.

Similar curves for Steels 16, 17, 19 and 20 are shown in Fig. 8. The curves for V2-type impact tests are also included in this figure, for Steels 17 and 19. It is to be noted that the transition temperatures of the latter two steels determined by cleavage-tear tests agree with the respective transition temperatures determined by V2 and V5 impact tests, in the same manner as in cases of other steels discussed in the preceding two paragraphs. It is to be noted further that the transition temperature of Steel 17 determined by the use of the T2-type tensile coupon is likewise about the same as that determined by the use of the D-type tensile-bend, and types V2 and V5 impact coupons. This is significant, since it indicates that an edge-notched 2-in. wide rectangular tensile coupon could be effectively used for determining transition temperatures of steel plates.

Figure 8 also shows that the transition temperature

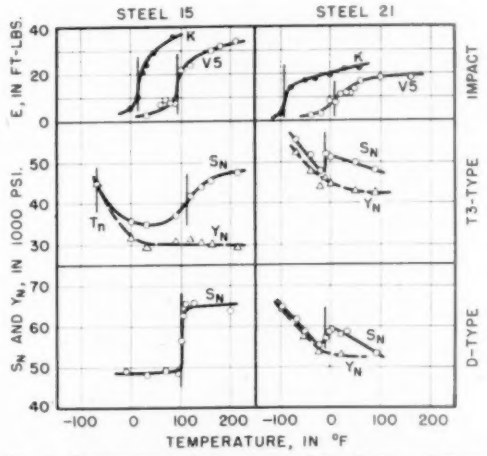

Fig. 7 Comparison of properties of Steels 15 and 21 at various temperatures as revealed by T3-type or tensile, and D-type or tensile-bend tests of the cleavage-tear group, and by types K and V5 tests of the impact group

Transition temperatures are indicated by short vertical lines

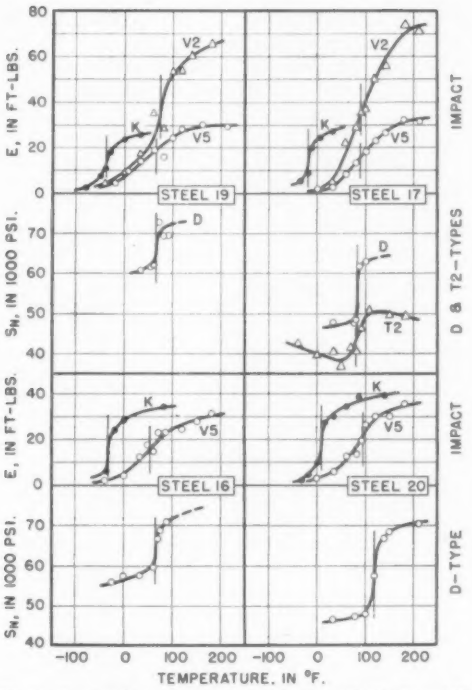

Fig. 8 Comparison of properties of Steels 16, 17, 19 and 20 at various temperatures as revealed by T2-type or tensile, and D-type or tensile-bend tests of the cleavage-tear group, and by types K, V2 and V5 tests of the impact group

Transition temperatures are indicated by short vertical lines

of Steel 16, which is a steel made by the fine grain melting practice, and that of killed Steel 19 determined by the tensile-bend tests likewise agree with the respective transition temperatures determined by types V2 and V5 impact tests. In the case of Steel 20, which is also a killed steel of $2^1/_4$ in. in thickness, the transition temperature revealed by V5-type impact tests was found to be about 25° F. higher than that shown by the cleavage-tear tests. This difference is undoubtedly due to the effect of thickness of the plate, since in the D-type coupon the plate was tested in full thickness, whereas in the V5-type impact coupon the thickness was only 0.394 in. Part of this discrepancy may also be due to the fact that the V5-type impact coupon was machined from the mid-thickness of the plate, thereby accentuating any non-homogeneity effects present. In all cases the K-type impact tests were found to show approximately 100° F. lower transition temperatures than those shown by the cleavage-tear tests. In this connection it should be noted that the magnitude of the drop in nominal stress values through the transition temperature range is appreciably less in case of the killed Steels 16 and 19 than that in case of the semikilled steels.

The effects of heat treatments, straining and aging of Steel 23 are presented graphically in Fig. 9. It is to be observed that a close agreement likewise exists between the V5-type impact and DX-type bend tests with respect to transition temperatures of Steel 23 in each of the strain-aged, water quenched and normalized conditions; also that the transition temperatures indicated by K-type impact tests for the steel in the above three conditions are approximately 70 to 110° F. lower than those shown by types V5 and DX tests for the respective conditions. In the annealed condition, the behavior of Steel 23 was found to be quite erratic in the bend test, and the transition temperature of the steel in this condition was estimated to be approximately 60° F. higher by this test than that indicated by the V5-type impact test. No irregularities were revealed by the V5-type impact tests on the steel in the annealed condition to account for this discrepancy. This observation is discussed further in a subsequent section of the paper.

Figure 10 shows the transition curves of Steel 23 determined by the use of types D and DX coupons, with machined as well as cold-pressed notches, and with fractures transverse and parallel to rolling. Transition curves for this steel are also included in this figure for fractures transverse and parallel to rolling, determined by use of types K, V2 and V5 impact coupons. In the lower portion of this figure are the respective curves for

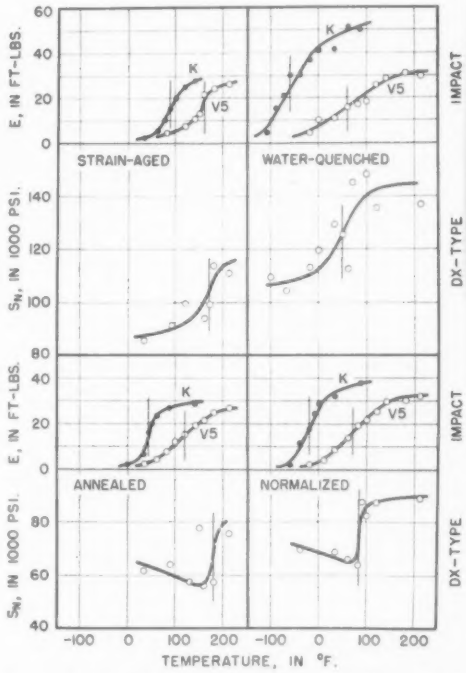

Fig. 9 Comparison of properties of Steel 23 at various temperatures and in various heat-treated and strain-aged conditions, as revealed by DX-type or bend test of the cleavage-tear group, and by types K and V5 tests of the impact group

Transition temperatures are indicated by short vertical lines

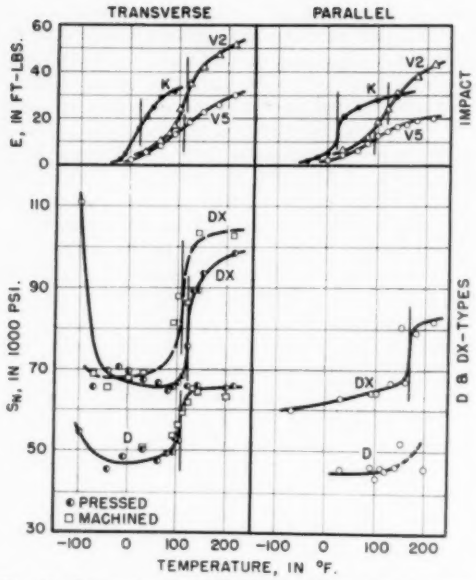

Fig. 10 Properties of Steel 23 at various temperatures, with fractures transverse and parallel to rolling, as revealed by DX-type or bend, and D-type or tensile-bend tests of the cleavage-tear group, and by types K, V2 and V5 tests of the impact group

The curves obtained by the use of pressed and machined notches are shown in lower left field. Transition temperatures are indicated by short vertical lines. (See reference 1 of text for data on D-type coupons with machined notches.)

tensile-bend and bend tests, and in the upper portion of this figure are the impact test curves. It is to be noted that with fractures transverse to rolling no substantial difference exists between the transition temperatures determined by D-type coupons containing machined or cold-pressed notches. It is also to be noted that with DX-type coupons, a difference of only 10°F. could be found between the transition temperatures revealed by these two types of notching. The impact tests for fractures transverse to rolling confirm these transition temperatures, with the same margin of difference as described in the preceding paragraphs. In the right-hand field of Fig. 10 are shown the curves for fractures parallel to rolling of the plate. In this case no definite transition temperature for this steel was established by the D-type tensile-bend tests, although a fairly definite transition temperature is revealed by the DX-type bend tests. The transition temperature shown by the bend tests is, however, about 50 and 75° F., respectively, higher than that shown by types V2 and V5 impact tests. It appears that due to rolling in one direction, some structural weakness is produced in steel plates in the direction parallel to rolling, which weakness is revealed by the cleavage-tear tests, but not by the impact tests.

Figure 11 depicts at about one-half natural size the appearance of brittle and ductile fractures obtained in the three types of cleavage-tear test coupons used in this investigation, on steels in the as-received condition and representing the thickness range of $^1/_4$ to $2^1/_4$ in. In the top row are shown the fractures obtained by the use of D-type or tensile-bend coupons in the $^1/_4$-in. thick Steel 21, $^{15}/_{16}$-in. thick Steel 23 and $2^1/_4$-in. thick Steel 20. In the bottom row of this figure are the fractures in DX-type or bend, and T3-type or tensile coupons of Steel 23. In all cases the brittle fracture is at the left and the ductile fracture at the right. The ductile fractures being produced by shear, run at an angle of about 45° to the rolled surface and give the respective sections a thicker appearance. It is to be observed that Steel 21 shows some buckling in the coupon fractured in the ductile manner. The ductile fracture in Steel 20 changed its course through an angle of about 45° after it had penetrated approximately one-half of the depth of the test coupon.

DISCUSSION

In the conventional tensile test the stress being unidirectional no rigid restrictions are present against flow at right angles to the principal stress. Consequently, an exaggerated index of ductility is obtained in this test, which ductility is normally measured by percentage elongation or reduction in the cross-sectional area of the test coupon. That the extent of ductility or toughness shown by steels in the tensile test is exaggerated is evident by the fact that steels showing good ductility in this test may, under certain conditions, fail in structures of monolithic or massive design, exhibiting very little or practically no permanent deformation, due to the fact that in such structures multiaxial stress conditions are present which restrict lateral flow or deformation.

With the idea of simulating service conditions as much as possible, notched test bars have been used in the past, since it has been known that a notch localizes the stresses, creates a state of multiaxial stress and thereby restricts flow or permanent deformation in the section under test. However, the behavior of a given steel is not the same with the use of notches of all types, so the problem of designing the proper notch to reflect the exact performance of steel under service conditions is quite involved. Theoretical and experimental evidence now available indicate that the theory of elasticity is inadequate to solve this problem. As the section is loaded to fracture some permanent deformation occurs at the base of most notches, which deformation produces relaxation of stresses and therefore renders inapplicable formulas based on the elastic theory. The extent of such permanent deformations depends on the acuity of the notch and type of the material as well as the geometry of the section involved. Useful information on these details and on approximate evaluation of the effect of the geometry of the notch and of the section can be obtained by tests.[1]

It has generally been assumed that the notch sensitivity of a given steel can only be evaluated by determining the relative ductilities exhibited by the steel in notched and unnotched sections. Impact and other

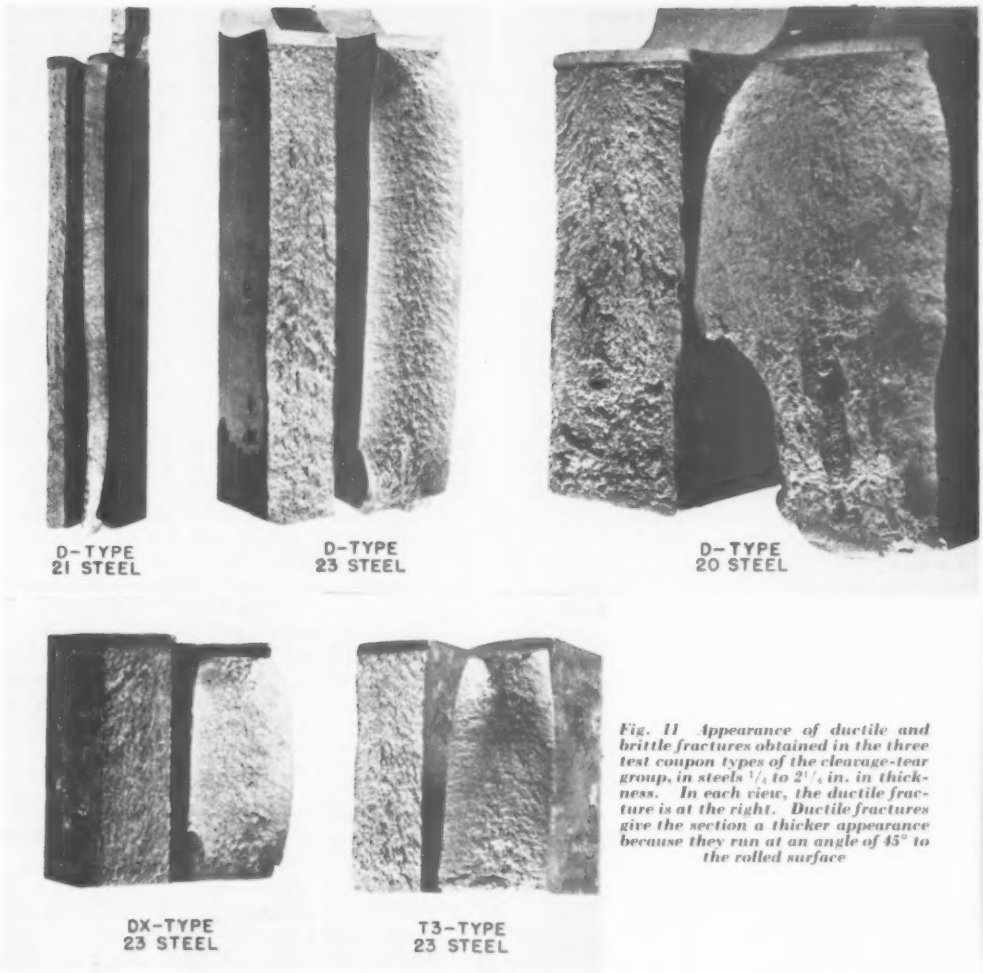

Fig. 11 Appearance of ductile and brittle fractures obtained in the three test coupon types of the cleavage-tear group, in steels $^1/_8$ to $2^1/_4$ in. in thickness. In each view, the ductile fracture is at the right. Ductile fractures give the section a thicker appearance because they run at an angle of 45° to the rolled surface

tests are used for this purpose. For determining transition temperatures, these tests are conducted at various temperatures; the average temperature at which a marked decrease occurs in the energy of fracturing is designated as the transition temperature of the steel. It has also been generally assumed that steels which show low transition temperatures by these tests are less notch sensitive than those showing high transition temperatures. However, the data presented in this paper show that with the use of cleavage-tear tests the notch sensitivity characteristics, including transition temperatures, can be more directly evaluated by determining nominal yield and breaking loads or stresses over a temperature range in which changes occur in these values. In this case the average temperature at which the rate of change in the nominal breaking stress values is greatest is considered as being the transition temperature. By determining the nominal yield and nominal breaking strengths, a second important temperature is established, which is the T_n temperature. This is the highest temperature at which the nominal yield and breaking strengths coincide. The T_n temperature is another phase of notch sensitivity, and like the transition temperature, it is different for different steels. The cleavage-tear tests appear to be simpler, yield more information on notch sensitivity characteristics, and are free from difficulties involved in evaluating local ductility.

That the exact evaluation of ductility is complicated is illustrated by Fig. 6. A study of this figure shows that although the transition temperature of a given steel is indicated to be the same by the three different types of tests of the cleavage-tear group, the T_n temperature is not indicated to be the same by these different tests. It is obvious then that the extent of ductility or deformation involved in tensile-bend, tensile and bend tests is not the same. The geometry of the notch, and the rate

of crosshead separation of the tensile machine used were the same in all these tests, but the geometry of the coupons and the manner of load application were not the same. In view of this it is apparent that aside from other factors the geometry of the test coupon and the manner of load application modify considerably the ductility of a steel. This situation could not be revealed as clearly by measuring local deformations on the different types of test coupons used, say at or a certain distance below the notch, unless these measurements were taken at such relative locations which were known to yield comparable results. The cinematographic method of determining nominal yield and breaking strengths used in cleavage-tear tests is independent of such variations in local ductility, since it deals with the behavior of the test coupon as a whole and not with local deformations.

For discussing the effect of notch geometry variations reference is made to Fig. 5. The curves for the D-type tensile-bend, and V5-type impact tests included in this figure show clearly that the influence of changes in the notch root radius is quite different in these two types of test. For the tensile-bend tests, shown in the left field of this figure, transition temperatures T_0 decrease progressively as the notch root radius is increased, the logarithm of T_0 in ° F. absolute being a straight line function of the logarithm of R. Accordingly, assuming that this relationship is valid for values of R approaching the dimensions of the head of cleavage cracks, the transition temperatures of the steels tested could be found by extrapolation to be within the range of about 150° to 250° F. for cases where a cleavage crack acts as the notch.[2]

In the impact tests the same straight line relationship of T_0 and R exists only for notch root radii 0.01 in. or greater. For notch root radii 0.01 in. or smaller, T_0 = constant, that is, transition temperatures become independent of notch root radii. The work of other investigators[5] using another type of test seems to confirm the latter observation. Since impact test are insensitive to changes in values of R in cases where R <0.01 in.. it appears that in such cases the effective value of the stress concentration factor is also constant and independent of R. These observations indicate that the ratio of notch root radius to coupon depth cannot be used as a basis for computing stress concentration at the notch.

The experimental data presented in this paper show that under the standard test conditions used and for plates 1/2 to 1 in. in thickness, a close agreement is obtained in the determination of transition temperatures of steels by the cleavage-tear and V-notch Charpy impact tests, and that the cleavage-tear transition temperatures can be correlated with those determined by the use of keyhole-notched Charpy impact coupons. A summary of the transition temperatures established by these tests for the steels investigated is given in Table 10 for ready reference. For convenience of comparison, the differences in the transition temperatures revealed by the use of types D, DX, K, V2 and V5 test coupons are included in Table 10. It is to be observed from Section I of this table that in the as-received condition and for fractures transverse to rolling, the transition temperatures of a given steel determined by the tensile-bend, tensile and bend tests of the cleavage-tear group and by types V2 and V5 tests of the impact group agree with each other within an average margin of dif-

Table 10—Comparison of Transition Temperatures Determined by Cleavage-Tear and Impact Tests (Transition Temperatures Are in ° F.)

I. Steels in as-received condition

Steel	Thickness, in.	Quality	Cleavage-tear test types D	T3	DX	Impact test types V5	V2	K	T_0 differences, ° F. D − K	V5 − K	D − V2	D − V5
15	1	Semikilled	103	115	...	95	...	18	85	77	...	8
16	1	Killed	68	...	...	55	...	−32	100	87	...	13
17	1	Semikilled	83	80[a]	...	90	90	−17	100	107	−7	−7
18	1	Semikilled	105	105	105	90	...	5	100	85	...	15
19	1	Killed	65	...	...	65	75	−38	103	103	−10	0
20	2 1/4	Killed	120	...	...	95	...	10	110	85	...	25
21	1/4	Rimmed	−10	−10	...	10[b]	...	−90[b]	80	100	...	−20
22	1/2	Rimmed	92	...	...	80	...	−15	107	95	...	12
23	15/16	Semikilled	110	110	120	110	115	20	90	90	−5	0
23	15/16	Semikilled[c]	[d]	...	170	95	120	20	...	75	...	...
		Average difference for 1/2 to 2 1/4-in. thick plates:							97.3	94.0	...	11
		Average difference for 1/2 to 1-in. thick plates:							98	92	−7	7

II. Steel 23 in conditions indicated

Condition	Cleavage-tear test type DX, pressed	DX, milled	Impact test types V5	V2	K	To differences, ° F. DX − K	V5 − K	DX − V5
As-received, fracture transverse	120	110	110	115	20	100	90	10
Stretched 5% at room temperature	150	...	135	...	80	70	55	15
5% stretched and aged 1 hr. at 400° F.	170	...	160	...	88	82	72	10
Water quenched	...	50	60	...	−60	110	120	−10
Water quenched and aged 1 hr. at 400° F.	...	50	65	...	−60	110	125	−15
Normalized	85	...	70	...	−20	105	90	15
Annealed	180(?)	...	120	...	45	135(?)	75	60(?)
As-received, fracture parallel[e]	170	...	95	120	20	150	75	75
Average difference for all conditions except annealed, with fractures transverse to rolling:						96	91	11

T_0 = Transition temperature.
[a] T2-type coupons; all others in this column, T3-type. [b] Subsize coupon (6 mm. thick by 10 mm. wide by 50 mm. long). [c] Fracture parallel to rolling; all others, fracture transverse to rolling. [d] Indeterminate by D-type coupon.

ference of about 0 ± 7° F. The transition temperatures determined by the V2-type and V5-type impact tests are on the average about 7° F. higher and about 7–10° F. lower, respectively, than those determined by the cleavage-tear tests. Inasmuch as the notch root radius is the same in types V2 and V5 coupons, the observed difference in the transition temperatures revealed by the use of these two types of coupon is to be attributed to the relative shallowness of the notch in V2-type coupon, and to the resultant tendency of the latter coupon to undergo more bending during fracturing than the V5-type coupon.

It is also to be observed from Section I of Table 10 that under the same test conditions the transition temperatures of a given steel determined by the K-type impact test (standard keyhole notch Charpy) are on the average 92–98° F. lower than those determined by the cleavage-tear and types V2 and V5 impact tests. However, the differences in transition temperatures were somewhat greater than the above averages in Steels 20 and 21, whose thickness was $2^1/_4$ and $^1/_4$ in., respectively. The transition temperatures of Steels 20 and 21 determined by the D-type test, are respectively, about 25° F. higher and about 20° F. lower than the transition temperatures determined by the V5-type impact test. Some such discrepancy would be expected, since in cleavage-tear tests the steels are tested in their full thickness and thereby the effect of thickness is integrated in the test results obtained whereas in impact tests the coupon from plates of different thicknesses is machined to a standard size, for which reason the effect of thickness is lost. Transition temperatures of steels with fracture parallel to rolling is discussed in the next paragraph.

In Section II of Table 10 is given a similar summary of transition temperatures of Steel 23 in various conditions of heat treatments, including annealing, normalizing, water quenching, water quenching and aging, cold working, and of cold working and aging; also for fractures parallel to rolling, with the steel in as-received condition. It is to be noted that for a given condition the transition temperatures of this steel determined by the bend, and V5-type impact tests similarly agree with each other. The discrepancies observed in the case of the steel in annealed condition and for fractures parallel to rolling may be due to the probability that impact coupons being small are incapable of storing sufficient potential energy to be sensitive to anisotropic properties of steel plates or to structural conditions favorable for cleavage crack propagation. The cleavage-tear tests, in which coupons of greater mass are used, possess good sensitivity and reveal these conditions. As is indicated by the data given in Table 3 and the summary shown in Section II of Table 10, the transition temperature of Steel 23, for fractures parallel to rolling, is indeterminate by tensile-bend tests, within the temperature range covered. According to bend tests, the transition temperature of this steel with the fracture parallel to rolling is about 50° F. higher than that with fracture transverse to rolling of the plate. The annealed steel, according to cleavage-tear tests, is more susceptible to cleavage cracking, probably because annealing induces equilibrium conditions to prevail and produces structural rearrangements which render the cleavage planes more favorable for cleavage crack propagation.

In this connection attention is called to the fact that greater resistance is offered to propagation of cleavage cracks by Steel 23 in water-quenched condition than in the as-received condition. This can be explained by the assumption that the cleavage planes are probably distorted or discontinuous in the quenched steel because of the martensitic transformation and the resultant lattice distortions brought about by this heat treatment. Normalizing likewise increases resistance of steel to cleavage crack propagation, but not to the same extent as water quenching. Section II of Table 10 shows that in the water-quenched condition the transition temperature of Steel 23 is about 60° F. lower, and in the normalized condition about 40° F. lower than that in the as-received condition. In contrast to this, in the annealed condition the transition temperature of this steel is about 60° F. higher than that in the as-received condition. The superiority of the water-quenching heat treatment persists to subzero temperatures. That this treatment appreciably increases the resistance of the steel to fracturing is shown by the two curves in the right-hand field of Fig. 3, also by the curves in Fig. 9.

It is apparent from the discussion in the preceding paragraphs that, within certain limits, the relationship of transition temperatures determined by impact and cleavage-tear tests can be expressed concisely by a simple equation, such as that proposed by the author previously,[1, 2] this equation being:

$$T_c = T_0 - T_r \qquad (1)$$

in which

T_c = Transition temperature in ° F. absolute, determined by keyhole-notched standard Charpy impact coupon (K-type, Fig. 1).

T_0 = Transition temperature in ° F. absolute, determined by D-type cleavage-tear test coupon shown in Fig. 1, containing the standard notch of root radius R in. (Types T2, T3 and DX cleavage-tear coupons can also be included, since T_0 obtained by these and D-type coupons is approximately the same.)

T_r = $T_0 - T_c = T_0 C(1/R)^d$

C and d = Constants for a given steel in a given condition.

Equation 1 appears to be valid for correlating transition temperatures determined by the cleavage-tear and impact tests, except in the following three cases: first, fractures parallel to rolling; second, annealed steel; and third, where the thickness of plates tested is outside the range of $^1/_2$–1 in. With these exceptions, the average difference in the transition temperatures determined by the D-type cleavage-tear and K-type impact tests, as has been stated, is in round figures about 100° F., the maximum individual variations being within the

range of 85–103° F. Therefore, the average value of T_s for this case is about 100° F. This value of T_s holds also for correlating the transition temperatures determined by the K-type and types V2 and V5 impact tests, since T_0 determined by types V2 and V5 impact and D-type tests is about the same (0 ± 7° F.). Evidently, the notch root radius used in types V2 and V5 tests (R = 0.01 in.) and that used in cleavage-tear tests (R = 0.0015 in.) are equivalent in the effect of modifying transition temperatures. These values of T_s apply to the steels tested in as-received condition, also to Steel 23 in normalized, water quenched, water quenched and aged, 5% cold worked, and 5% cold worked and aged conditions. For steel plates 1/4 to 2 1/4 in. in thickness, the average margin of correction to be applied to the value of T_s is about 0 ± 15° F. The effects of section thickness and other conditions are treated in more detail in Appendix A, by equations of more general type.

This agreement of the cleavage-tear and types V2 and V5 impact tests with respect to transition temperatures, and correlation of these with the transition temperatures determined by the K-type impact test are remarkable in view of the facts that: in impact tests the transition temperature determinations are based on energy of fracturing, whereas in cleavage-tear tests they are based on nominal breaking strength; the section depth subjected to fracturing, also the rate and manner of load application are quite different in the different types of tests used; the plate thickness range covered is fairly wide, it being 1/4 to 2 1/4 in.; the steels tested are numerous and of different qualities, rimmed, semikilled and killed grades being included; and the as-received condition as well as strain-aged and various heat-treated conditions are represented in the tests. It follows then that, so far as modifying transition temperatures is concerned, the effect of the notch root radius is relatively much greater than that of the loading rate or section mass.

Although transition temperatures are important, cleavage-tear tests indicate that in order to evaluate the quality of a given steel with respect to notch sensitivity, it is necessary to determine other characteristics, in addition to the transition temperature. One of the most important of these characteristics is the T_n temperature shown in Fig. 6 for Steels 18 and 23, which, as has been stated is like the transition temperature, in that it is different for different steels. Since at the T_n temperature the nominal yield and nominal breaking strengths coincide, below this temperature no permanent deformation occurs in the steel on a macroscopic scale, with the entire test section considered as a unit. This is indicated by the several curves in Figs. 3 and 6. It is important to determine the T_n temperature because between this temperature and the transition temperature some ductility or toughness is present in the steel which, although small, is probably sufficient in a number of cases to level off stress peaks and thereby to prevent possible cleavage failures. Figure 6 shows that the transition temperature of Steel 18 is 105° F. and that of Steel 23 is 110° F., both according to tensile-bend test, yet by the same test the T_n temperature of Steel 18 is 32° F. and that of Steel 23 is −40° F. On the basis of transition temperatures alone, Steel 18 would appear to be of a somewhat superior quality than Steel 23, whereas the respective T_n temperatures indicate Steel 23 to be much superior to Steel 18.

The T_n temperature then has practical significance and appears to account for the observation that the number of cleavage failures of all-welded ships increases greatly when service temperatures decrease from levels which are below the transition temperatures of ship plates. Transition temperature curves established by impact tests, or by tests which evaluate notch sensitivity by transition temperatures alone, do not offer an explanation for this increase of failures, since no significant changes are indicated by the latter curves below the transition temperature range. Cleavage-tear tests, on the other hand, show that as the temperature decreases the breaking strength decreases and the yield strength increases, indicating that the probability of failure by cleavage increases with decrease of temperature.

Another important phenomenon revealed by the cleavage-tear tests consists in the fact that in presence of sharp notches the breaking strengths of steels immediately below the transition temperature are considerably less than those at temperatures lower or higher than this range. This is shown by the several S_N vs. T curves of Fig. 6. It is probable that the rise in breaking strengths of steels at temperatures above transition temperatures is effected by the work hardening and deformation associated with ductile fractures. The similar rise in the breaking strengths observed at the lower temperatures is probably in sympathy with the rise in the yield strength, the exact details of which mechanism are not clearly known. It appears worth while in future work to investigate just to what extent and how far down the temperature scale this rise in breaking strengths of notched sections of steels continues. Nevertheless, it is apparent from the present work that steels require considerably higher stress to fracture at say −100° F. than at temperatures moderately below their transition temperature range. This property of steels can undoubtedly be utilized advantageously in those low temperature applications where deformations encountered are entirely of elastic type.

On the basis of the test data presented in this paper, it is possible to determine the transition temperatures as well as the T_n temperatures by a number of rather simple cleavage-tear tests, such as the DX-type bend, or the T-type tensile tests. The T-type coupon is a rectangular bar, either 2 or 3 in. in width, which is notched on one edge and broken in the conventional manner in a tensile-testing machine, at various temperatures. By plotting the yield and breaking loads against temperature the above two important temperatures could be established. Similar data could also be obtained by the use of DX-type bend coupon. These coupons are certainly less costly to prepare than the Charpy impact

coupon. The T-type coupons have the further advantage of being adaptable for testing tubular products in sections with full wall thickness. However, more fundamental information is obtained by the D-type tensile-bend coupons, in which pronounced eccentric loading is used. The combination of straight tension and bending present in, and the greater mass of, the latter type of test coupon imparts greater sensitivity to the testing procedure, since while the section below the notch is rigid, the maximum stress is developed at the notch by eccentric loading. Furthermore, Fig. 6 also shows that the tensile-bend test reveals the highest T_n temperature, which indicates that this test exhibits greater sensitivity for determining the T_n temperature than the T-type tension or DX-type bend tests. In this connection attention is called to the observation previously discussed that different T_n values are indicated by different methods of stressing and by different geometries of test coupon. Further investigations are required in order to establish the details of the mechanism involved.

Aside from their several advantages which have been discussed, cleavage-tear tests are applicable both within and without the ranges in which impact tests are sensitive, yielding within the sensitive range of impact tests the same transition temperatures as those obtained by V-notched impact tests (R = 0.01 in.). Therefore, cleavage-tear tests could be considered as expanding the usefulness of impact tests since, in the ranges where impact tests become inapplicable or insensitive, continuity of results could be maintained by the use of cleavage-tear tests.

It is obvious from the data presented that the temperatures at which most of our present steels of pressure vessel and structural qualities in the as-rolled condition show appreciable ductility are considerably above atmospheric temperatures, if sharp notches are present. Such notches could be present in some structures in the form of small cracks, defects or discontinuities, from which cleavage fractures could develop at atmospheric temperatures, with damaging results. Therefore, considerable improvements in the quality of steels are necessary in order to immunize them against this type of failure, under all conditions of service, the actual cost of which improvements is not known definitely at present. Steels made by the present fine-grain melting practice may not be the answer in all cases. Cross rolling and heat treatments are helpful, but may be found not effective for all applications or services. For some services, alloy additions may be required. In any case, these facts should be carefully considered and the design and inspection details worked out so that structural discontinuities and defects are avoided.

The killed steels as a group were found to be comparatively less notch sensitive than the other steels of the same thickness, as has been discussed. The two killed Steels 16 and 19 were found to have considerably lower transition temperatures than the semikilled steels of the same thickness. The third killed steel, Steel 20, being $2^1/_4$ in. thick was found to behave about the same as the semikilled and rimmed steels so far as transition temperature is concerned, which behavior may be attributed to the effect of thickness of this plate. There was not sufficient stock left in killed steels to determine their behavior with respect to T_n temperatures.

No definite relationship was found between grain size and transition temperature, within the range of grain sizes investigated. Steels 16 and 19 have considerably different grain sizes, but approximately the same transition temperature, the grain size of Steel 16 being 8–9 and that of Steel 19 being 6–7. Another example is Steel 17 which has about the same grain size as the other semikilled steels, but a somewhat lower transition temperature than the latter steels. Likewise, no definite relationship was found between the ratio of manganese to carbon contents and transition temperatures. This becomes evident by comparing the transition temperature of Steel 18 (Mn/C = 3.1) and the transition temperatures of the other steels whose Mn/C is appreciably less than 3. In terms of its T_n temperature, Steel 18 cannot be considered as being superior to the other semikilled grades.

Previous investigations[1] have indicated that increasing the depth of sections beyond that of D-type tensile-bend coupon does not materially change the nominal breaking strengths or transition temperatures. The present investigation has shown that the effect of thickness and variations in notch root radii can be evaluated by cleavage-tear tests. Inasmuch as the effects of these variations in the geometry of the section and of notch can be evaluated by cleavage-tear tests, it appears that the latter tests offer a more suitable method of determining notch sensitivity characteristics of steels than impact tests. Impact tests have the advantage of requiring a comparatively small amount of metal for making the tests, but it is indicated that they can be used only with important limitations. By the use in a routine manner of some of the simple cleavage-tear tests described, it may be possible to determine which particular melting, furnacing and rolling procedures will sufficiently improve the quality of steels so as to make them more suitable for building of massive structures or pressure vessels of monolithic construction.

Aside from considerations of tensile strength, ductility and transition temperature, it seems that a suitable steel for above applications would be one which would suffer through the transition temperature range not more than say 30% loss in its notch toughness as evaluated by standard impact tests or not more than about 10% drop in its breaking strength as measured by cleavage-tear tests, and whose T_n temperature is at least 50° F. lower than the lowest temperature encountered in the intended service. These requirements could in part be expressed in terms of the ratio of breaking to yield strengths at the service temperature, as determined by suitable notched-bar tests. It is probable that steels meeting such a specification could be more economical and more serviceable than those specified solely on the basis of meeting an arbitrary transition temperature.

SUMMARY AND CONCLUSIONS

1. Notch sensitivity characteristics of nine open-hearth rimmed, semikilled and killed steels of structural and pressure vessel qualities, in the form of plates and representing the thickness range of $^1/_4$ to $2^1/_4$ in., were evaluated by the use of several types of notched bar tests. The objects of this investigation were to explore the possibility of correlating the results yielded by these different tests and of determining whether or not any of the steels and testing methods would show any relative superiority over the others.

2. Two groups of tests were used in this investigation: cleavage-tear and impact. The cleavage-tear group consisted of tensile-bend or tear, tensile, and bend tests, using a different type of test coupon for each type of test; the impact group consisted of three series of tests using standard Charpy impact coupons, 10 mm. sq. by 55 mm. long, but a different type of notch for each series, these being the standard Charpy keyhole notch and V notches of 45° included angle with 2 and 5 mm. depths. Both groups of tests were conducted within the temperature range of −100 to 260° F., and the changes in certain properties of steels through this range were determined. The criterion of notch sensitivity used in cleavage-tear tests was the changes in the nominal yield and nominal breaking strengths, most of which values were determined cinematographically, and that in impact tests was the change in the energy of fracturing.

3. The transition temperatures of a given steel tested in the as-received condition and with fractures transverse to rolling were found to be substantially the same by all tests of the cleavage-tear group and by the V-notched impact tests, the average difference between the different tests being about 0 ± 7° F. for steel plates of $^1/_2$ to 1 in. in thickness. This average difference was somewhat greater for plates $^1/_4$ to $2^1/_4$ in. in thickness, it being about 0 ± 15° F. A similar agreement was found in the transition temperatures of Steel 23 determined by the tests mentioned in normalized, water quenched, water quenched and aged, 5% cold worked, and 5% cold worked and aged conditions.

4. No agreement was obtained in the transition temperatures determined by the cleavage-tear and impact tests on the steel in annealed condition, or with fractures parallel to rolling of the plate. This disagreement is attributed to the inability of the impact tests to reveal anisotropic properties of steel plates and the vulnerability of annealed steel to cleavage crack propagation. The transition temperature of Steel 23 with fractures parallel to rolling was found to be indeterminate by the tensile-bend tests within the temperature range covered, and that determined by the bend test was estimated to about 50° F. higher than indicated by the V-notched impact tests. The transition temperature of this steel in the annealed condition, determined by the bend test was found to be at least 60° F. higher than that indicated by the V-notched impact tests.

5. The transition temperatures determined by cleavage-tear tests, under the conditions and with the exceptions discussed in paragraphs 3 and 4, were found to be about 100° F. higher than those indicated by the use of standard keyhole notched Charpy coupons, with an average variation of about ±15° F. from this figure.

6. The transition temperature of Steel 23 was lowered about 40° F. by normalizing and about 60° F. by water-quenching heat treatments, as compared with that in the as-received condition. Similarly, 5% cold working, and 5% cold working and aging raised the transition temperature of this steel about 30° and 50° F., respectively.

7. The cleavage-tear tests, in addition to being free from the main limitations of the impact tests, disclose an important feature of notch sensitivity, which is the temperature at which the yield and breaking strengths coincide, and designated T_n temperature. This temperature has practical significance, since below the transition temperature some ductility is exhibited by steels, the extent of which ductility is not the same for all steels. Although this ductility is small, it may be sufficient to level off the stress peaks in some cases and thereby prevent possible failures. This ductility decreases as the temperature decreases and becomes zero at the T_n temperature, hence the importance of determining the T_n temperature.

8. Decreasing the notch root radius R in cleavage-tear tests increases progressively the transition temperature T_0, the logarithm of T_0 in ° F. absolute being a straight line function of the logarithm of R, within the range tested. In impact tests the same relationship of T_0 to R prevails for values of R 0.01 in. or greater, but for values of R 0.0008 to 0.01 in. the transition temperature remains the same and is independent of the notch root radius. It is probably for this reason that the same transition temperatures were obtained by the V-notched impact coupons with 0.01 in. notch root radius as by the cleavage-tear coupons in which the notch root radius used was 0.0015 in.

9. It is indicated that the ratio of notch root radius to coupon or notch depth could not be used as a basis for evaluating stress concentration and stress gradients at the notch. The data presented show clearly that, so far as modifying transition temperatures is concerned, the notch root radius has in general a much greater effect than the depth of the metal below the notch.

10. Of the two V-notched impact coupons used in this investigation, the V5-type appears to be less subject to errors introduced by bending during fracturing than the V2-type. This is attributable to the relative shallowness of the notch in V2-type coupon.

11. The insensitivity of impact tests to changes in notch root radii R in cases where R is less than 0.01 in. can be utilized for reducing the scatter in impact test results. This can be accomplished by specifying the notch root radius dimension for V-notched impact coupons to be say $0.01 + 0 - 0.005$ in., instead of specifying 0.01 in. as is the present practice. Tests show that

this change has no effect on the determination of transition temperatures or on the impact values.

12. On the basis of the test data presented, equations are derived for correlating within certain limits the transition temperatures determined by cleavage-tear and impact tests.

13. The pressing method used for notching cleavage-tear test coupons does not appreciably affect transition temperature determinations.

14. The killed steels as a group show a somewhat lower transition temperature than the rimmed and semi-killed grades of the same thickness.

15. No definite relationship was found with respect to grain size and transition temperature. Similarly no relationship was found between the transition temperature and the ratio of manganese to carbon contents of a given steel.

16. Cleavage-tear tests were found to have the following inherent advantages over impact tests:

(*a*) A more complete picture of notch sensitivity is obtained, since by cleavage-tear tests the yield and breaking strengths can be evaluated and the T_s temperature or the temperature of coincidence of the yield and breaking strengths can be determined. These are not revealed by impact tests.

(*b*) Cleavage-tear tests have greater sensitivity than impact tests for evaluating anisotropy and responsiveness of steels to cleavage crack propagation in the annealed condition; for evaluating the effect of notches with small root radii; and for integrating the effect of thickness.

(*c*) Transition temperatures of as-rolled steel plates determined by cleavage-tear tests, within certain limitations of thickness and conditions of heat treatment discussed, can be correlated with results obtained by conventional impact tests. In the ranges in which impact tests are insensitive or inapplicable, cleavage-tear tests could be used for obtaining continuity of test results.

(*d*) Cleavage-tear tests can be made on an ordinary tensile testing machine, the test coupons used being simple and costing much less to prepare than those used in impact tests. Of the cleavage-tear tests, the tensile-bend test using the D-type coupon is considered to be a more sensitive test for determination of nominal yield and breaking stresses and the T_s temperature. For routine work, types T2, T3 or DX coupons can be used for testing of plates and possibly also tubular products.

17. The importance of routine tests is discussed and desirable improvements in steel quality are described.

ACKNOWLEDGMENT

The author is grateful to J. N. Pew, Jr., of Sun Oil Co. Management, for his sponsorship and encouragement of this project. This investigation was conducted by the Metallurgical Section, Engineering Div. of Sun Oil Co., under the direction of the author. Of the Metallurgical personnel, special thanks are due to J. E. Carney for valuable assistance and for developing the pressing method of notching used; to W. M. Brotherston for supervising preparation of most of the test coupons; to H. N. Seward for conducting the cleavage-tear tests; to J. L. Truman for developing the special milling method of notching used; and to V. Montgomery for conducting the impact tests. The cooperation given by the Sun Shipbuilding and Drydock Co. is gratefully acknowledged. Special thanks are due to D. E. Robertshaw of the latter Company for his assistance in conducting the cleavage-tear tests. The author is also indebted to W. F. Brown, Jr., of the Physical Research and Development Division of Sun Oil Co., for suggestions and for reviewing the manuscript.

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Appendix A

The equation given in the discussion section of the paper,

$$T_s = T_0 - T_0C(1/R)^d = T_0 - T_s \qquad (1)$$

includes only one variable term of notch root radius, this being the notch root radius R of the D-type cleavage-tear coupon; the notch root radius of impact coupons is assumed to be a constant and conforming to a standard dimension (K-type). This was done for the sake of simplicity. In order to obtain an equation of a more general type, the notch root radii of cleavage-tear and impact test coupons should both be considered. To accomplish this, reference is made to Fig. 5 of the paper. As is shown in this figure for tensile-bend tests the logarithm of transition temperatures in ° F. absolute is a straight line function of the logarithm of the notch root radius used. The same relationship is indicated to be true in V5-type impact tests for notch root radii of 0.01 in. or larger. For notch root radii smaller than 0.01 in., the transition temperature in the latter tests is a constant and independent of notch root radius, as indicated by Fig. 5. These relationships can be expressed by the equations given below, in which:

T_0 = Transition temperature in ° F. absolute determined by use of D-type coupon containing a notch of standard depth ($^3/_{16}$ in.) with 45° included angle and a root radius of R_0 in. The use of types DX, T2 and T3 coupons (see Fig. 1 of paper) could also be included,

since with the same notch root radius these coupons show the same T_0 as the D-type coupon.

T_t = Transition temperature in ° F. absolute determined by Charpy impact tests using V5-type coupon containing a notch of root radius R in. (see Fig. 1 of paper). The use of types K and V2 impact coupons could also be included without introducing any serious error.

a, b, Q, U and W = Constants for a given steel in a given condition.

f and g = Coefficients as defined by Equation 8.

t = Correction factor for section thickness: for plate thickness range $^1/_2$–1 in., $t = 1$; for plates less than $^1/_2$ in. thick, $t < 1$; and for plates thicker than 1 in., $t > 1$.

From Fig. 5 it is apparent that for tensile-bend tests the equation expressing T_0 as a function of R_0 is as follows:

$$\log T_0 + a \log R_0 = \log Q \qquad (2)$$

the equivalent of which is:

$$T_0 = Q(1/R_0)^a \qquad (3)$$

In Equation 3 the reciprocal of R_0 is used because of the negative slope of the T_0 vs. R_0 curves (Fig. 5).

Similarly, for impact tests the following equation expresses T_t as a function of R_t, with values of R_t 0.01 in. or greater:

$$\log T_t + b \log R_t = \log U \qquad (4)$$

the equivalent of which, as in the case of Equation 3, is the following:

$$T_t = U(1/R_t)^b \qquad (5)$$

The equation expressing T_t as a function of R_t, for values of $R_t = 0.01$ in. or smaller, is as follows:

$$\log T_t = \log W \qquad (6)$$

or

$$T_t = W \qquad (7)$$

By inserting proper coefficients and combining Equations 5 and 7, Equation 8 is obtained, which expresses T_t as a function of R_t for all values of R_t within the range of 0.0008–0.085 in. investigated:

$$T_t = fU(1/R_t)^b + gW \qquad (8)$$

in which $f = 1$ and $g = 0$, if $R_t = 0.01$ in. or larger; and $f = 0$ and $g = 1$, if $R_t = 0.01$ in. or smaller.

As has been indicated in the paper, the effect of thickness is not disclosed by impact tests, so that Equation 8 should be modified by a thickness term t, as follows:

$$T_t = t[fU(1/R_t)^b + gW] \qquad (9)$$

An equation relating T_0 to T_t can then be derived by combing Equations 3 and 9:

$$T_0 = \frac{T_t Q(1/R_0)^a}{t[fU(1/R_t) + gW]} \qquad (10)$$

By means of Equation 10 the transition temperature obtainable with the use of D-type coupon containing a notch of 45° included angle, $^3/_{16}$ in. depth and a root radius of R_0 in., for a given steel in a given condition, can be computed from the corresponding transition temperature T_t obtained by the use of the standard Charpy coupons containing a 45° notch of 5 mm. depth and a root radius of R_t in., or vice versa, if the numerical values of the constants Q, U, a, b and t are known or evaluated by tests.

In this investigation the standard notch root radii used for cleavage-tear and V5-type impact tests were 0.0015 and 0.01 in., respectively. For these conditions Equation 10 can be simplified to the following form:

$$T_0 = \frac{T_t Q(1/0.0015)^a}{tU(1/0.01)^b} \qquad (11)$$

Since under standard test conditions the transition temperatures determined by cleavage-tear and V5-type impact tests are approximately the same for a given steel plate within the thickness range of $^1/_2$–1 in., that is, T_0 is approximately equal to T_t, and $t = 1$, it is apparent that the value of the term $Q(1/0.0015)^a/U(1/0.01)^b$ in Equation 11 is approximately 1.

For cases in which $R_0 = R_t = R = 0.01$ in. or larger, and the plate thickness involved is within the range of $^1/_2$–1 in., Equation 10 can be simplified to:

$$T_0 = T_t(Q/U)(1/R)^{a-b} \qquad (12)$$

The exponents a and b are the slopes of the transition temperature vs. notch root radius logarithmic curves for cleavage-tear and impact tests, respectively, and Q and U are the characteristic constants for the respective tests, the numerical values of which, for a given steel, can be determined by experiments.

Spot Welding of Heavy-Gage Structural Steel

Satisfactory procedures have been developed for the spot welding of heavy-gage structural steel up to 1/2 in. in thickness. It is desirable to temper the spot in the heavier thicknesses

by Ernest F. Nippes and Robert F. Underhill

SUMMARY

IT IS possible to satisfactorily spot weld 1/8-in. pickled, and 3/16-, 1/4-, 3/8- and 1/2-in. gritblasted mild structural steel. In order to weld the 1/4-, 3/16- and 1/4-in. materials, such that the normal tension to shear ratios exceed 45%, it is necessary to temper the spot weld in the material. Tempering can be done satisfactorily by cooling the spot weld and subsequently reheating to the proper tempering temperature by the passage of additional current while the material is still between the welding electrodes. The recommended conditions for spot welding the five gage thicknesses of mild structural steel and for spot welding and tempering the 1/8, 3/16 and 1/4 in. thicknesses are given.

In addition, it was found that the electrode-to-metal interface temperature produced during the spot-welding of 3/8-in. gritblasted mild structural steel is comparatively the same whether the material is spot welded according to pulsation welding practices or continuous welding practices, the total welding time in each case being approximately the same.

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Scheduled for presentation at the Thirtieth Annual Meeting, A.W.S., Cleveland, Ohio, week of Oct. 17, 1949.

INTRODUCTION

This program, approved and guided by the Resistance Welding Research Committee and by the Subcommittee for Spot Welding of Steels, was financially supported by the Welding Research Council of the Engineering Foundation. The object of this investigation was the study of the spot-welding conditions for pickled 1/8, and gritblasted 3/16-, 1/4-, 3/8- and 1/2-in. mild structural steel.

MATERIAL

The chemical composition and mechanical properties of each of the gages of open-hearth mild steel used are shown in Table 1.

EQUIPMENT

A Federal 175-kva. press-type, toggle-operated resistance welder was used for spot welding. Welding current measurements were made by means of an Esterline-Angus recording ammeter. Variations in the welding current due to change in secondary transformer impedance or line voltage were compensated for by a current regulator.

The welding electrodes which were used are classified according to R.W.M.A. as Group A, Class 2. The electrodes were cooled with water maintained at 48–60° F.

GENERAL PROCEDURE

For the preliminary studies of optimum welding force and the effect of welding current upon weld diameter for various welding times, the standard size normal tension and shear samples, as recommended in the *R.W.M.A. Handbook*,[1] were not used. Instead, the test samples were cut to a length approximately equal to the recommended widths of the mechanical property test samples. Prior to welding, the various gages of mild structural steel, except the 1/8 in., were alundum gritblasted to remove the mill scale. The 1/8-in. material was supplied with a pickled and oiled surface, which was vapor-degreased with tetrachlorethylene and swabbed with carbon tetrachloride to remove the oil prior to welding. Superficial grinding with a 100-grit emery belt was used to clean the surfaces of the samples which rusted during storage.

Normal tension and shear test samples were welded and tested in later sections of the work in accordance with the recommendations given in the *R.W.M.A. Handbook*.[1]

DISCUSSION OF RESULTS

Welding and Forging Force

The initial phase of the investigation was the determination of the optimum welding force that should be used in order to produce sound welds, that is, crack-free and porous-free welds. It was decided that sound welds are necessary in mild structural steel, particularly if they are subject to fatigue-type stresses. By means of metallographic and radiographic examinations, it was found that, with the maximum welding and forging force attainable in this laboratory, 13,500 lb., it was not possible to produce porous free welds in

Table 1—Chemical Analysis and Mechanical Properties

Heat No.	*Gage thickness, in.*	*C, %*	*Mn, %*	*P, %*	*S, %*	*Tensile strength, psi.*	*Yield point, psi.*	*Elongation, %*	*Bend test*
04K061	1/8	0.22	0.47	0.008	0.032	64,580	...	...	...
04K061	3/16	0.22	0.47	0.008	0.032	64,560	...	...	...
29D181	1/4	0.24	0.45	0.015	0.033	62,900	40,260	27.5	OK
29D181	3/8	0.24	0.45	0.015	0.033	...	...	...	...
01J662	1/2	0.29	0.50	0.009	0.032	68,340	42,920	32.2	OK

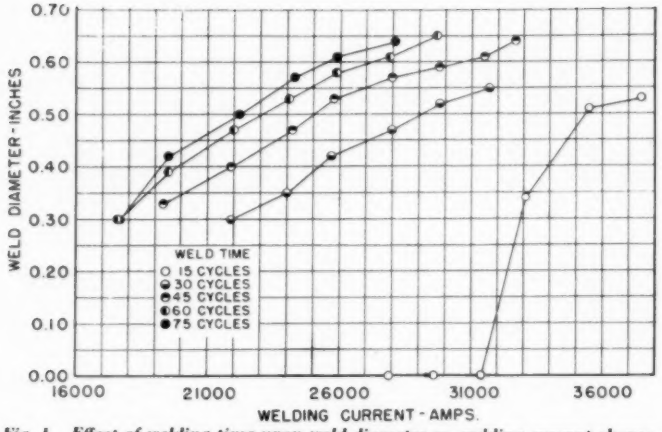

Fig. 1 Effect of welding time upon weld diameter vs. welding current characteristic curves for $^1/_8$-in. mild steel

the $^1/_8$-, $^3/_8$- and $^1/_2$-in. specimens. To minimize porosity, this maximum available force of 13,500 lb. was applied to the $^3/_8$- and $^1/_2$-in. material just prior to the completion of the weld cycle. Since an average sheet separation of 7% resulted when using the 13,500-lb. force upon the welded $^1/_8$-in. material, a combined welding and forging force of 12,500 lb. was selected for minimizing the porosity and sheet separation in the $^1/_8$-in. spot-welded mild steel.

The welding force studies pertaining to the $^1/_8$-in. mild steel material showed that a welding force of 6000 lb. sustained after the completion of the welding time produced sound welds. A forging force of 4000 lb., in addition to the 6000 lb. welding force, must be applied to spot welds in $^3/_{16}$-in. material to produce the same results. The nominal weld diameters produced in the various gages of mild steel for the purpose of establishing the optimum welding or optimum welding and forging force are given in Table 2.

Welding Time

With the optimum welding force established for the spot welding of each of the mild steel thicknesses, the optimum welding time for each was determined. The method used for determining the optimum welding time was the study of the effect of welding current upon weld diameter for a variation in welding time. The welding currents used were those which ranged between the formation of an incipient type weld to a weld equal to or slightly larger than the diameter of the electrode tips, or to a weld which expelled during the welding cycle.

Graphic representations of the effect of welding current upon weld diameter for various welding times for the five different gage thicknesses are shown by Figs. 1–6. These graphs show that with an increase in weld time beyond the minimum selected, the increase in weld diameter for a given increase in welding current becomes less.

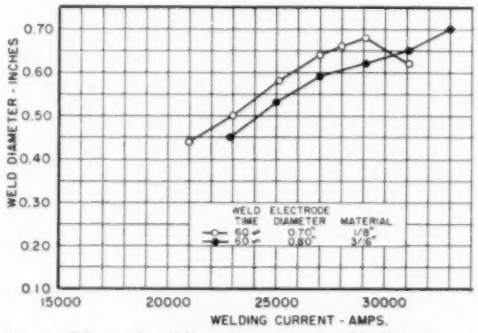

Fig. 2 Effect of welding time upon weld diameter vs. welding current characteristic curves for $^1/_8$- and $^3/_{16}$-in. mild steel

As an example, a welding current of 27,900 amp. and a weld time of 15 cycles produced no fusion weld in the $^1/_8$-in. material. An increase in welding time to 30 cycles and a welding current of 28,000 amp. resulted in a weld diameter of 0.47 in. However, by increased welding times of 45, 60 and 75 cycles, maintaining approximately the same welding current, the weld diameter increased only to 0.57, 0.61 and 0.64 in., respectively.

It is to be noted from Fig. 6 that the total weld time for the $^1/_2$-in. material consisted of a preheat time and a welding time, the preheat time being one-half of the welding time. This welding sequence was necessary in order to produce the maximum diameter welds desired, with the welding current capacity attainable in this laboratory.

Since the increase in weld diameter becomes proportionately less with a given increase in welding time for a given welding current, the selection of an optimum welding time is simplified. An optimum welding time might be defined as one which will not produce a great variation in weld diameter for a varation in welding current. In addition, if the selected welding time should vary slightly because of the lack of proper control, this slight variation in welding time for a given welding current should not produce a great variation in weld diameter. On the basis of this definition, the optimum welding times, as shown in Table 2 for the five thicknesses of mild structural steel, were selected.

It will be noted that there is an additional weld diameter vs. welding current characteristic graph, Fig. 2, for the $^1/_8$- and $^3/_{16}$-in. material. Since the original nominal weld diameters for both these thick-

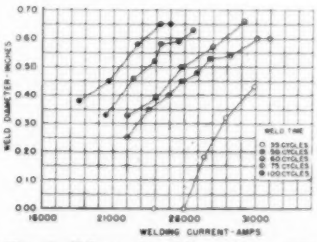

Fig. 3 Effect of welding time upon weld diameter vs. welding current characteristic curves for $^3/_{16}$-in. mild steel

Table 2—Welding Data

Gage, in.	*Nominal weld diameter, in.*	*Electrode tip diameter, in.*	*Total weld time, cycles*
$^1/_8$	0.55	0.625	45
$^3/_{16}$	0.62	0.687	60
$^1/_4$	0.82	0.875	240
$^3/_8$	0.91	0.937	450
$^1/_2$	1.03	1.125	600

nesses, as shown previously in Table 2, were found to be inadequate for reasons which will be explained in a later section, the electrode tip diameter was increased from 0.625 to 0.700 in. for the 1/8-in. material and from 0.687 to 0.800 in. for the 3/16-in. material. A comparison of the resultant weld diameter vs. welding current characteristic curves of Fig. 2 with those of Fig. 1 indicates that for the 1/8-in. material smaller weld diameters were produced by the larger electrode diameter for a given welding current, whereas, with the 3/16-in. material, Figs. 2 and 3, larger weld diameters were produced by the larger electrode diameter. The slopes of curves in Fig. 2 for both the 1/8- and the 3/16-in. material are similar to the slopes of the corresponding curves of Figs. 1 and 3. It is to be noted in Fig. 2 that the weld diameter vs. welding current characteristic curve for the 1/8-in. material decreased somewhat at the welding current value of 31,100 amp. This decrease is attributed to expulsion during the welding of the test sample.

Strength-Current Characteristics

In order to establish the optimum welding conditions for spot welding the 1/8-, 3/16-, 1/4-, 3/8- and 1/2-in. mild structural steel, strength-current characteristic curves were determined in accordance with a previous investigation in this laboratory.[2]

During the present investigation, the experimental work for establishing the welding conditions consisted of welding groups of three normal tension and three shear samples at various values of welding current. The welding current range for each of the five thicknesses of mild steel was that used for the study of the effect of welding current upon weld diameter for various welding times. The welding force, welding time and welding sequence for each of the materials were those given in the previous discussion concerning these variables. The forging force, which had to be applied to some materials to eliminate or minimize porosity, was applied approximately 10 cycles before the completion of the weld.

The normal tension and shear strengths determined for each group of as-welded samples of each gage thickness are given in Tables 3–7. The results of these mechanical tests are the average of three specimens, unless otherwise specified. Included with the mechanical properties are the average weld diameter measurements of either the normal tension or shear test specimens. The average weld diameter measurements in most cases consist of an average of average measurements; that is, the weld diameter measurement for each sample of any one group is an average of two weld diameter measurements at 90° to each other. This average weld diameter was then averaged with the other of its group to give the average

Table 3—Average Mechanical Properties for As-Welded 1/8-in. Pickled Mild Structural Steel

Group	Specimen Type	Welding Current Amperes	% Indentation	% Sheet Separation	Strength Lbs.	Weld Diameter Inches	Failure Type
1	N. T.	19,500	1.3	1.5	445	0.31**	Shear
	Shear	19,500	1.2	1.1	4,555	----	Shear
2	N. T.	21,400	1.5	0.8	880	0.31	Shear
	Shear	21,400	1.3	0.9	6,292	----	Shear
3	N. T.	23,600	1.2	1.6	1,320	0.41	Shear
	Shear	23,600	1.9	2.4	8,300	----	Shear
4	N. T.	25,600	1.6	2.9	1,350	0.49	Shear
	Shear	25,600	1.6	3.3	9,258	----	Shear
5	N. T.	27,300	2.0	4.9	1,630	0.55	Shear
	Shear	27,300	2.1	5.3	9,850	----	Shear
6	N. T.	29,400	2.7	6.8	1,760*	0.62	Shear
	Shear	29,400	3.1	9.3	10,983	----	1 Plug and 2 Shear
7	N. T.	31,588	3.9	11.6	2,430	0.66	1 Plug and 2 Shear
	Shear	31,400	4.3	10.9	11,450	----	Plug
8	N.T.****	32,600	20.2	7.9	2,700*	0.66***	1 Shear and 2 Plug
	Shear****	32,500	19.9	9.0	10,958	----	Plug

Electrode Tip Diameter-0.625 in.
* Average of 2 Tests Only
** Incipient Fusion Welds
***Measurement for One Weld Only
****Two of the Three Welds Expelled During Welding

Table 4—Average Mechanical Properties for As-Welded 3/16-in. Gritblasted Mild Structural Steel

Group	Specimen Type	Welding Current Amperes	% Indentation	% Sheet Separation	Strength Lbs.	Weld Diameter Inches	Failure Type
1	N. T.	22,100	2.3	0.3	1,560	0.25	Shear
	Shear	22,000	2.3	1.0	8,850	----	Shear
2	N. T.	23,500	2.5	0.8	2,660	0.37	Shear
	Shear	23,500	2.4	1.6	10,530	----	Shear
3	N. T.	25,000	2.5	1.3	2,500	0.44	Shear
	Shear	25,000	2.7	1.2	10,440	----	Shear
4	N. T.	26,400	2.3	2.5	3,130	0.51	Shear
	Shear	26,400	2.6	2.6	10,530	----	Shear
5	N. T.	27,800	3.1	3.7	2,850	0.58	Shear
	Shear	27,800	2.8	3.3	12,220	----	Shear
6	N. T.	29,400	3.2	3.9	3,550	0.61	Shear
	Shear	29,400	3.3	4.1	13,960	----	Shear
7	N. T.	31,100	3.9	5.3	3,810	0.65	Shear
	Shear	31,100	3.8	5.2	15,860	----	Shear
8	N. T.	32,000	4.7	7.6	4,060	0.69	Shear
	Shear	32,000	5.0	6.7	14,370	----	Shear

Electrode tip diameter - 0.687 in.

Table 5—Average Mechanical Properties for As-Welded 1/4-in. Gritblasted Mild Structural Steel*

Group	Specimen Type	Welding Current Amperes	% Indentation	% Sheet Separation	Strength Lbs.	Weld Diameter Inches	Failure Type
1	N. T.	20,100	3.5	2.4	5,230	---	Shear
	Shear	20,200	3.3	3.0	17,800	0.59	Shear
2	N. T.	22,000	4.1	3.3	5,270	----	Shear
	Shear	21,800	3.6	3.9	19,800	0.65	Shear
3	N. T.	24,000	4.1	4.2	6,350**	----	Shear
	Shear	24,000	3.8	4.5	21,500	0.73	Shear
4	N. T.	25,800	4.5	4.3	6,630	----	1 Partial Plug & 2 Shear
	Shear	25,700	4.3	5.9	22,800	0.81	Shear
5	N. T.	27,500	5.0	6.3	7,420	----	Shear
	Shear	27,600	5.1	6.8	25,500	0.86**	Shear
6	N. T.	29,200	5.8	8.1	7,680**	----	Plug
	Shear	29,200	5.7	8.5	25,400	0.93**	Shear

Electrode Tip Diameter - 0.875 - in.

*Ground on a 100 Grit Belt to Remove Adhering Rust Prior to Welding

**Average of Two Tests Only

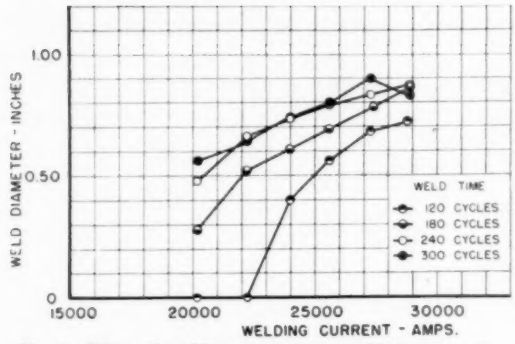

Fig. 4 Effect of welding time upon weld diameter vs. welding current characteristic curves for 1/4-in. mild steel

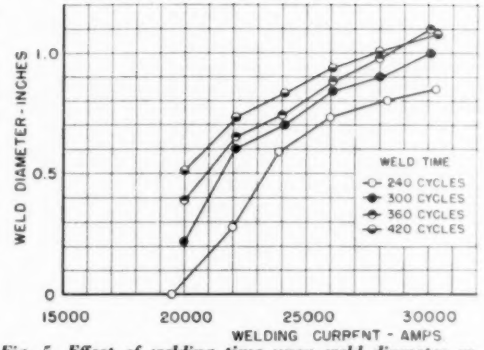

Fig. 5 Effect of welding time upon weld diameter vs. welding current characteristic curves for 3/8-in. mild steel

Table 6—Average Mechanical Properties for As-Welded 3/8-in. Gritblasted Mild Structural Steel

Group	Specimen Type	Welding Current Amperes	% Identation	% Sheet Separation	Strength Lbs.	Weld Diameter Inches	Failure Type
1	N. T.	20,200	1.2	0.2	11,260	----	Shear
	Shear	20,200	0.8	0.5	15,580	0.42	Shear
2	N. T.	21,900	1.4	0.9	13,200	----	Shear
	Shear	21,800	1.0	0.6	23,600	0.62	Shear
3	N. T.	24,000	1.2	1.3	14,750	----	Shear
	Shear	24,000	1.3	1.8	29,700	0.77	Shear
4	N. T.	26,000	1.2	2.7	15,700	----	Shear
	Shear	26,000	1.4	2.0	33,780	0.88	Shear
5	N. T.*	28,300	2.0	3.1	17,100	----	Plug
	Shear	28,300	1.8	3.3	37,800	0.98**	1 Plug & 2 Shear
6	N. T.	30,200	3.4	4.7	20,000	----	Shear
	Shear	30,200	3.0	4.5	42,600	1.06	Shear

Electrode Tip Diameter - 0.937 in.

* Slight Extrusion of Metal Between the Welded Plates

** Average of Two Tests Only

weld diameter data as recorded in Tables 3–7.

The average normal tension and shear strengths for each group of samples were plotted against the average welding current for that group. The resultant strength-current characteristic curves for as-welded 1/8-, 3/16-, 1/4-, 3/8- and 1/2-in. mild structural steel are shown by Figs. 7–11.

The normal tension curves for these gages show a gradual increase with an increase in welding current. The same is true of the shear strength curves for the 3/16-, 3/8- and 1/2-in. material. However, the shear strength curve for the 1/4-in. material decreases slightly at the maximum welding current value, while the curve for the 1/4-in. material is nearly constant for the last two values of welding current. This decrease in shear strength of the 1/4-in. material is probably the result of ex-

Table 7—Average Mechanical Properties for As-Welded 1/2-in. Gritblasted Mild Structural Steel*

Group	Specimen Type	Current - Amperes Preheat	Current - Amperes Weld	% Indentation	% Sheet Separation	Strength Lbs.	Weld Diameter In.	Failure Type
1	N. T.	20,000	25,300	1.4	0.3	12,280	-	Shear
	Shear	20,000	25,100	1.7	0.5	32,700	0.58	Shear
2	N. T.	20,000	27,200	1.7	0.5	19,070	-	Shear
	Shear	20,000	27,100	1.7	0.8	34,900	0.69	Shear
3	N. T.	20,100	29,100	1.7	1.0	25,220	-	Shear
	Shear	20,000	29,100	1.8	1.0	36,500	0.76	Shear
4	N. T.	20,000	31,100	1.9	0.7	26,500	-	Shear
	Shear	20,000	31,100	2.0	1.3	45,600	1.03	Shear
5	N. T.**	19,900	33,600	2.1	2.4	27,300	-	Shear
	Shear	19,900	33,600	2.0	1.6	53,300	1.14****	1 Shear & 2 Plug
6	N. T.***	20,000	35,700	2.4	2.4	28,730	-	Shear
	Shear	20,000	35,500	2.5	2.8	55,050	1.04*****	1 Plug & 2 Shear

Electrode Tip Diameter - 1.125 In.

* Short circuiting occurred between the plates of each weld because of warped plates.

** Slight extrusion of metal between the welded plates.

*** Slight extrusion of metal between two of the three of the welded plates

**** Result of one test only.

***** Average of two tests only.

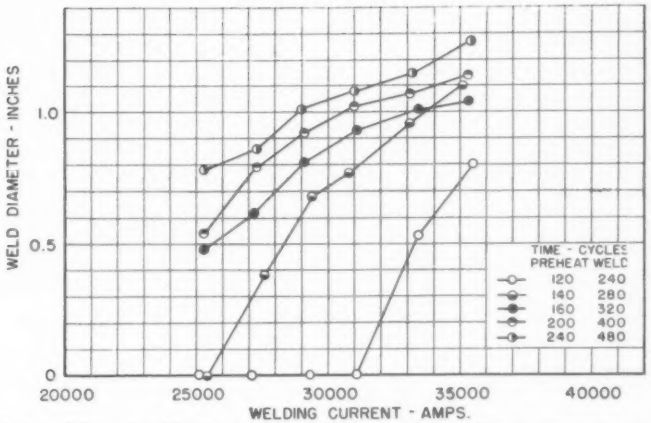

Fig. 6 Effect of welding time upon weld diameter vs. welding current characteristic curves for 1/2-in. mild steel

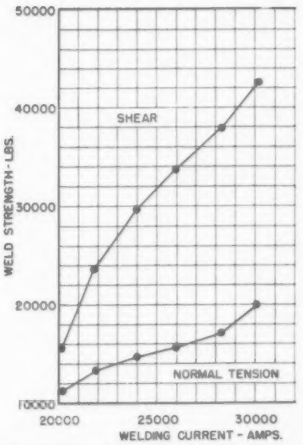

Fig. 10 Strength-current characteristic curve for gritblasted 3/8-in. mild steel

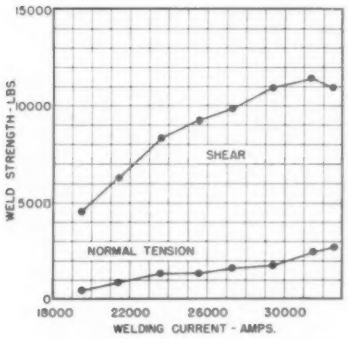

Fig. 7 Strength-current characteristic curve for pickled 1/8-in. mild steel

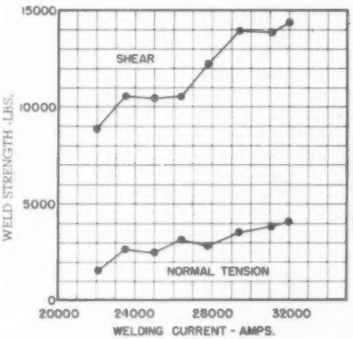

Fig. 8 Strength-current characteristic curve for gritblasted 3/16-in. mild steel

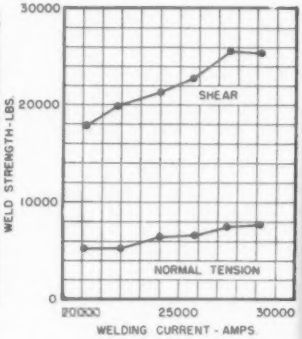

Fig. 9 Strength-current characteristic curve for gritblasted 1/4-in. mild steel

Table 8—Summary of As-Welded Conditions

	Material Gauge Thickness						
	1/8"	1/8"	3/16"	3/16"	1/4"	3/8"	1/2"
Welding Electrodes	0.625	0.70	0.687	0.800	0.875	0.937	1.125
Tip Diameter - in.	10	10	10	10	10	10	10
Dome Radius - in							
Truncated Cone Taper-	30	30	30	30	30	30	30
(degrees)	----	---	----	----	---	----	13,500
Preheat Force - lbs.	6,000	6,000	6,000	6,000	6,500	6,500	6,500
Welding Force - lbs.	-----	-----	----	----	-----	----	20,000
Preheat Current - amps.	27,300	28,200	29,400	32,000	25,800	26,000	31,100
Welding Current - amps	-----	-----	----	----	----	----	200
Preheat Time - cycles	45	60	60	60	240	360	400
Welding Time - cycles	6,000	6,000	10,000	10,000	12,500	13,500	13,500
Forging Force - lbs.	0.55	0.63	0.61	0.71	0.81	0.88	1.03
Weld Diameter - in.							
Normal Tension Strength-	1,630	1,990	3,550	4,400	6,630	15,700	26,500
lbs. (normal)	9,850	10,930	13,960	14,920	22,800	33,780	45,600
Shear Strength-lbs. (normal)							
Normal Tension/Shear	16.6	18.2	25.4	29.4	29.1	46.5	58.2
Strengths Ratio - %							

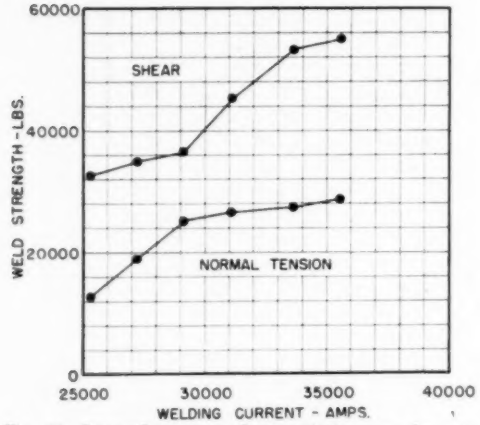

Fig. 11 Strength-current characteristic curve for grit-blasted 1/4-in. mild steel

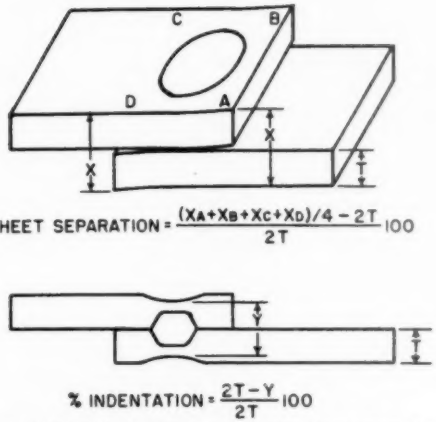

Fig. 12 Method for determining per cent sheet separation and per cent indentation

pulsion of two of the welds made at this current value.

In order to establish the optimum welding conditions for the five gages of mild structural steel from the strength-current characteristic curves, two factors were taken into consideration. First, the welding current was selected so that, if it varied slightly during production welding of the material, the normal tension and shear strengths of the material would not be appreciably changed. Secondly, the normal tension to shear ratio was considered satisfactory if it exceeded the arbitrary value of 45%.

On the basis of these two factors, a summary of as-welded conditions, for the five different gages of mild structural steel, is found in Table 8. It may be seen from the normal tension to shear ratio values, that the 1/8-, 3/16- and 1/4-in. materials did not meet the 45% specification. These low ratios are the result of the formation of brittle structural constituents at the periphery of the weld nuggets. These are either martensitic in the case of the 1/4 in., or a mixture of martensitic, pearlitic and possibly ferritic constituents in the case of the other two thicknesses of mild steel. Further discussion of this microstructural condition will be given in the section entitled "Tempering."

Sheet Separation and Indentation

The welding force caused sheet separation outside of the weld area and the electrode indentation into the sheets or plates during the welding cycle. Measurements were made of the as-welded samples of each gage thickness to determine whether the 10% sheet separation and 5% indentation arbitrary maximum limits were exceeded. A diagrammatic sketch, showing the method by which these values were determined, is given in Fig. 12. The average sheet separation and indentation measurements for the as-welded normal tension and shear samples are given in Tables 3–7. The results of these measurements indicate that the sheet separation and indentation limits were not exceeded for any of the five gage thicknesses of mild structural steel welded according to the welding conditions specified in Table 8. Although the 1/4-in. material was forged with a total force of 13,500 lb. during preliminary tests, it was found that the indentation measurements exceeded the maximum limit of 5% in a majority of cases, as shown in Table 5. Therefore, the forging force applied to the welds in the 1/4-in. material was established at 12,500 lb.

Tempering

As pointed out in the previous discussion, the as-welded normal tension to shear ratios for 1/8-, 3/16- and 1/4-in. mild structural steel, welded according to conditions of Table 7 did not meet the desired minimum limit of 45%. A preliminary study was made to determine whether an increase in welding time would improve the mechanical properties of the 1/8- and 3/16-in. materials. An increase in welding time would increase the over-all temperature of the material surrounding the weld nugget, and would decrease the cooling rate after completion of the weld. Thus, the formation of the complete martensitic structure about the periphery of the weld nugget could be prevented if the cooling rate did not exceed the critical cooling rate for the material. However, by means of Vickers hardness surveys across the transverse sections of welds[2] made at

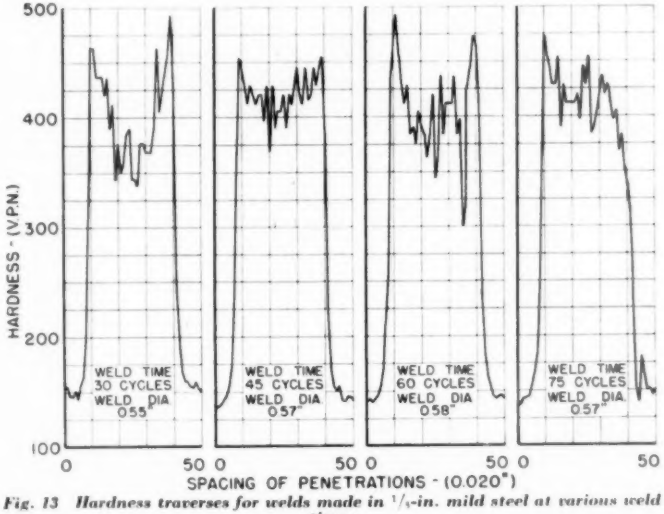

Fig. 13 Hardness traverses for welds made in 1/4-in. mild steel at various weld times

Table 9—Average Mechanical Properties for Welded and Tempered 1/8-in. Pickled Mild Structural Steel

Group	Specimen Type	Current - Amperes Weld	Temper	Temper/Weld Current Ratio	Strength Lbs.	% Ratio N.T. Shear Strength	Weld Diameter Inches	Failure Type
1	N. T.	25,000	----	-------	1,710			Shear
	Shear	25,000	----	------	9,870	17.3	0.56	Shear
2	N. T.	25,000	18,800	75.3	2,060			1 Partial Plug & 2 Plug
	Shear	25,000	18,800	75.3	10,330	20.0	0.55*	1 Shear & 2 Plug
3	N. T.	24,900	19,900	80.0	3,150			Plug
	Shear	25,100	20,100	80.3	10,780	29.2	0.56	Shear
4	N. T.	25,000	20,500	82.0	3,020			Plug
	Shear	25,000	20,500	82.0	11,000	27.4	0.56	1 Plug & 2 Shear
5	N. T.	25,000	20,900	83.7	3,770			Plug
	Shear	25,000	21,000	84.0	11,200	33.6	0.56	Shear
6	N. T.	25,000	21,500	86.0	4,410			Plug
	Shear	25,000	21,400	85.6	11,320	38.9	0.56	Shear
7	N. T.	25,000	22,100	88.4	2,930			1 Shear & 2 Plug
	Shear	25,100	22,100	88.2	11,390	25.8	0.55	Shear
8	N. T.	25,000	22,500	90.0	3,370			Plug
	Shear	25,000	22,500	90.1	10,750	31.3	0.56	Shear
9	N. T.	25,000	22,800	91.4	2,890			Plug
	Shear	25,000	22,900	91.5	10,950	26.4	0.56	1 Plug & 2 Shear

* Average of 2 tests only

various times in 1/8- and 3/16-in. material, it was found that an improvement in microstructure should not be expected, as shown in Figs. 13 and 14. Increases in welding time from 30 to 75 cycles for the 1/8-in. material and 50 to 100 cycles for the 3/16-in. material, caused no appreciable difference in the hardness of the weld nuggets.

Therefore, tempering of the three different gage thicknesses was used for increasing the normal tension to shear ratio to 45%. In order to properly temper the materials, it was necessary to determine the optimum cooling time between the completion of the welding and start of the tempering. The cooling time for each material was determined on the basis of mathematical calculations[3] and previous experimental results from this laboratory.[4] The cooling times selected were confirmed by means of Vickers hardness surveys. Except for the 1/4-in. material, the samples of each gage thickness used for the hardness survey were welded according to the summary of as-welded conditions in Table 8, prior to cooling and tempering between the electrodes of the welding machine. Because of equipment limitations, it was necessary to increase the welding time for the 1/4-in. material from 45 to 60 cycles. The optimum cooling times for the 1/8-, 3/16- and 1/4-in. mild

Table 10—Average Mechanical Properties for Welded and Tempered 1/8-in. Pickled Mild Structural Steel

Group	Specimen Type	Current-Amperes Weld	Temper	Temper Weld Current Ratio	% Indentation	% Sheet Separation	Strength Lbs.	% Ratio N.T. Shear Strength	Weld Diameter Inches	Failure Type
1	N. T.	28,400	----	---	3.2	7.4	1,990			Shear
	Shear	28,200	----	---	3.1	7.0	10,930	18.2	----	Plug
2	N. T.	28,200	22,500	79.8	3.1	7.1	2,820			Plug
	Shear	28,200	22,500	79.8	2.7	6.5	11,570	24.4	0.62**	1 Shear & 2 Plug
3	N. T.	28,200	24,800	88.0	2.5	6.5	3,680			Plug
	Shear	28,200	24,700	87.6	2.5	7.3	12,650	29.1	0.63	Shear
4	N. T.	28,200	25,300	89.7	2.3	5.7	4,270			Plug
	Shear	28,200	25,200	89.4	2.5	6.4	12,980	32.9	0.63	Shear
5	N. T.	28,200	25,900	91.8	2.0	6.5	5,850			Plug
	Shear	28,200	25,900	91.8	2.4	6.6	13,370	43.7	0.64	Shear
6	N. T.	28,200	26,400	93.6	2.9	5.6	6,480			1 Plug & 2 Shear
	Shear	28,200	26,400	93.6	2.7	5.6	12,900	50.1	0.64	Shear
7	N. T.	28,200	27,200	96.5	2.3	5.3	3,970			Plug
	Shear	28,200	27,200	96.5	2.9	5.8*	12,120	32.7	0.63	Shear
8	N. T.	28,200	27,600	97.9	2.4	6.9	4,250			2 Plug
	Shear	28,200	27,600	97.9	2.3	6.0	12,220	34.7	0.62*	1 Plug & 2 Shear

* Average of 2 tests only

** Average of 1 test only

structural steel were found to be 200, 240 and 240 cycles, respectively.

Since previous experimental results[3] had shown that the tempering time for material 1/8 in. and greater in thickness should be twice that of the welding time, the tempering times established for the 1/8-, 3/16- and 1/4-in. materials were 120, 120 and 480 cycles, respectively.

The experimental results for the establishment of strength-current characteristics for these three gages of material are given in Tables 9–13. These include the average weld current, temper current, weld diameter, weld strengths, as well as the per cent ratios of temper current to weld current and of normal tension to shear strength for each group of welded samples. The graphic representation of these results are shown by Figs. 15–19.

Examination of the data will reveal that there are two sets of results for both the 1/8- and 3/16-in. materials, which correspond to two different nominal weld diameters for each thickness.

The following results were obtained from 1/8- and 3/16-in. materials which had been welded according to the conditions in Table 8 for the smaller weld diameters and subsequently tempered:

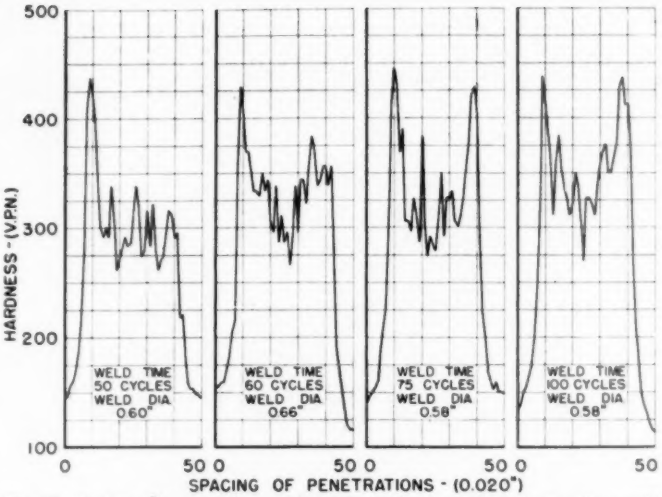

Fig. 14 Hardness traverses for welds made in 3/16-in. mild steel at various weld times

Gage thickness, in.	Nominal weld diameter, in.	Normal tension strength, lb.	Shear strength, lb.	Normal tension/shear ratio, %
1/8	0.55	3700	11,200	33.0
3/16	0.62	6500	18,500	35.1

Since these normal tension to shear ratios were considerably below the minimum value of 45%, the welding and tempering conditions were investigated for larger nominal weld diameters. As may be seen in Table 14, a compilation of optimum welding conditions, the normal tension to shear ratios for the larger weld diameters exceed the 45% arbitrary minimum limit. Table 14 also includes the data for welding and tempering the 1/4-in. material, which shows a favorable normal tension to shear ratio. The 3/8- and 1/2-in. data are repeated from Table 8, since the normal tension to shear ratios were satisfactory in the as-welded conditions.

As may be seen from Tables 9–13, the 10% sheet separation and 5% sheet indentation arbitrary maximum limits have not been exceeded for the samples welded and tempered according to the optimum conditions given in Table 14.

In order to show what effect tempering had upon the hardness of the welds in

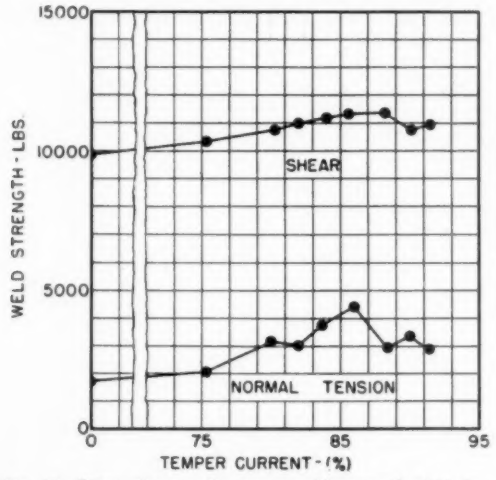

Fig. 15 Effect of tempering upon weld strength of 1/8-in. structural steel

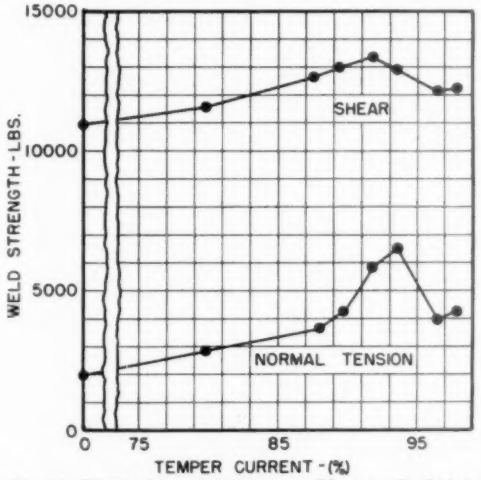

Fig. 16 Effect of tempering upon weld strength of 1/8-in. structural steel

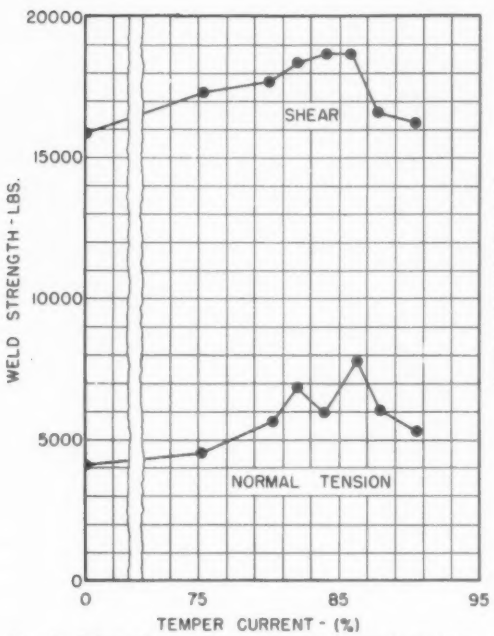

Fig. 17 Effect of tempering upon weld strength of $^3/_{16}$-in. structural steel

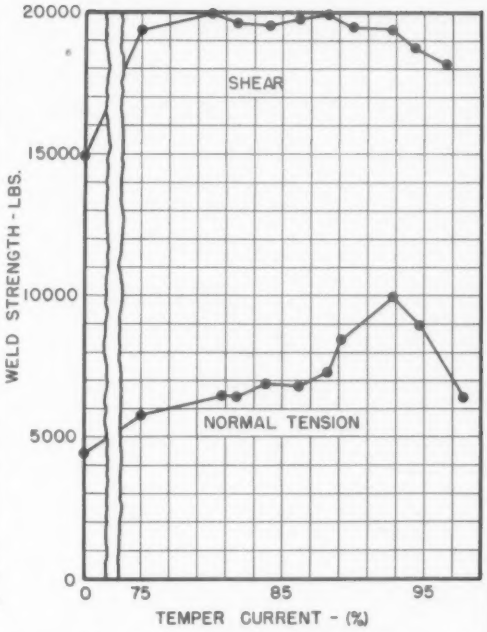

Fig. 18 Effect of tempering upon weld strength of $^3/_{16}$-in. structural steel

Table II—Average Mechanical Properties for Welded and Tempered $^3/_{16}$-in. Gritblasted Mild Structural Steel

Group	Specimen Type	Current-Amperes Weld	Current-Amperes Temper	Temper Current Weld Current Ratio	Strength Lbs.	% Ratio N.T./Shear Strength	Weld Diameter Inches	Failure Type
1	N. T.	29,400	----	---	4,100			Shear
	Shear	29,400	----	---	15,870	25.8	0.65	Shear
2	N. T.	29,300	22,000	75.2	4,570			1 Shear & 2 Plug
	Shear	29,300	22,000	75.3	17,300	26.4	0.63	Shear
3	N. T.	29,300	23,500	80.3	5,690			1 Plug, 1Shear & 1 Partial Plug
	Shear	29,300	23,400	80.0	17,700	32.1	0.62	Shear
4	N. T.	29,400	24,100	82.0	6,830			1 Shear & 2 Plug
	Shear	29,400	24,100	82.0	18,380	37.2	0.62*	Shear
5	N. T.	29,400	24,700	83.9	5,970			1 Shear & 2 Plug
	Shear	29,400	24,700	84.1	18,700	37.3	0.63*	1 Plug & 2 Shear
6	N. T.	29,500	25,500	86.3	7,800			1 Shear & 2 Plug
	Shear	29,400	25,200	85.8	18,700	41.8	0.63**	1 Shear & 2 Plug
7	N. T.	29,400	25,800	87.9	6,050			1 Plug & 2 Shear
	Shear	29,400	25,800	87.7	16,600	36.4	0.61	Shear
8	N. T.	29,400	26,600	90.5	5,300			Plug
	Shear	29,400	26,600	90.4	16,220	32.6	0.61	Shear

* Average of Two Tests

** Result of One Test Only

Table 12—Average Mechanical Properties for Welded and Tempered $^3/_{16}$-in. Gritblasted Mild Structural Steel

Group	Specimen Type	Current-Amperes Weld	Current-Amperes Temper	Temper Weld Current Ratio	% Indentation	% Sheet Separation	Strength Lbs.	% Ratio N.T. Shear Strength	Weld Diameter Inches	Failure Type
1	N. T.	32,000	----	---	2.8	3.9	4,400			Shear
	Shear	32,000	----	---	2.4	4.5	14,920	29.4	0.69	Shear
2	N. T.	32,000	24,000	75.0	2.0	4.3	5,770			Shear
	Shear	32,000	24,000	75.0	2.0	4.0	19,350	29.8	0.70	Shear
3	N. T.	32,000	25,800	80.7	1.9	3.8	6,450			Shear
	Shear	32,000	25,600	80.0	2.1	3.5	19,970	32.3	0.71	Shear
4	N. T.	32,000	26,200	81.8	2.0	3.7	6,420			Shear
	Shear	32,000	26,200	81.8	2.3	3.6	19,620	32.7	0.71	Shear
5	N. T.	32,000	26,800	83.8	2.2	3.1	6,880			Shear
	Shear	32,000	26,900	84.1	2.0	3.7	19,570	35.2	0.69	Shear
6	N. T.	32,000	27,600	86.2	2.4	3.1	6,800			Plug
	Shear	32,000	27,600	86.2	2.4	3.6	19,780	34.4	0.71*	1 Plug & 2 Shear
7	N. T.	32,000	28,200	88.2	2.4	3.6	7,300			Plug
	Shear	32,000	28,200	88.2	2.4	3.6	19,920	36.6	0.72**	1 Shear & 2 Plug
8	N. T.	32,300	28,800	89.2	2.4	3.6	8,450			Plug
	Shear	32,000	28,800	90.0	2.4	4.3	19,480	43.6	----	Plug
9	N. T.	32,300	29,700	92.8	---	---	9,930			Plug
	Shear	32,000	29,700	92.8	---	---	19,400	51.1	----	Plug
10	N. T.	32,000	30,300	94.7	2.7	3.7	8,920			Plug
	Shear	32,100	30,300	94.4	2.8	4.6	18,730	47.6	----	Plug
11	N. T.	32,000	31,200	97.8	---	---	6,400			Plug
	Shear	32,300	31,200	96.6	---	---	18,180	35.2	----	Plug

* Average of Two Tests. ** Result of One Test.

Table 13—Mechanical Properties for Welded and Tempered $^1/_4$-in. Gritblasted Mild Structural Steel

Group	Specimen Type	Current-Amperes Weld	Current-Amperes Temper	Temper Weld Current Ratio	% Indentation	% Sheet Separation	Strength Lbs.	% Ratio N.T. Shear Strength	Weld Diameter Inches	Failure Type
1	N. T.	25,800	----	---	2.5	5.5	6,200			Shear
	Shear	25,800	----	---	2.7	7.0	24,730	25.1	0.81	Shear
2	N. T.	25,900	20,700	79.8	1.9	2.3	9,100			Plug
	Shear	26,000	20,900	80.4	2.3	5.6	28,400	32.0	0.81	Shear
3	N. T.	25,900	22,200	85.8	1.8	3.1	11,920			Plug
	Shear	25,700	21,900	85.2	2.5	5.9	28,770	41.5	0.81	Shear
4	N. T.	25,900	22,700	87.6	1.9	3.2	18,100			Plug
	Shear	25,700	22,800	88.8	2.9	7.0	29,570	61.2	0.81**	1 Plug & 2 Shear
5	N. T.	25,900	23,100	89.2	1.7	2.4	14,770			Plug
	Shear	25,700	23,300	90.3	2.9	7.5	28,930	51.0	0.80**	1 Plug & 2 Shear
6	N. T.	25,900	23,600	91.2	1.9	3.5	15,030			Plug
	Shear	25,900	23,800	91.9	2.6	6.7	28,300	55.0	0.82	Shear
7	N. T.	25,700	24,400	94.9	3.1	7.6	12,980			Plug
	Shear	25,700	24,400	94.9	3.0	8.3	29,330	44.3	0.83	Shear
8	N. T.	25,700	25,000	97.3	3.4	8.4	14,580			Plug
	Shear	25,800	25,000	97.0	3.6	9.2	28,870	50.4	0.80**	1 Plug & 2 Shear

* Ground on a 100 Grit Emery Belt to Remove Adhering Rust Prior to Welding

** Average of 2 Tests Only

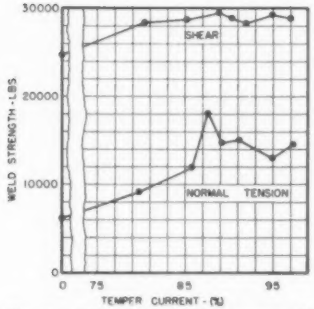

Fig. 19 Effect of tempering upon weld strength of 1/4-in. structural steel

1/8-, 3/16- and 1/4-in. mild structural steel, Vickers hardness traverse surveys were made, the results of which are shown by Figs. 20–24. Figures 21, 23 and 24 represent the changes in hardness which occur for 1/8-, 3/16- and 1/4-in. samples welded according to the conditions recommended for welding and tempering in Table 14, but tempered with different tempering currents, rated as percentages of the welding current. Figures 20 and 22 represent similar changes which occur for the 1/8- and 3/16-in. samples, but with smaller nominal weld diameters, 0.55 and 0.61 in., respectively.

The hardness surveys of particular interest are Figs. 21, 23 and 24. These indicate that tempering current is most effective in the range of 91.9 to 94.3% for the 1/8-in. material, 90.0 to 92.4% for the 3/16-in. material and 80.6 to 88.0% for the 1/4-in. material. The recommended tempering current ranges determined from the maximum normal tension to shear strength ratios in Tables 10, 12, and 13 for the 1/8-, 3/16- and 1/4-in. materials are given in Table 14. It may be readily seen that these two sets of tempering current ranges, found from two different mechanical properties, are in good agreement, especially in the small gage materials.

CONCLUSIONS

From the results of this investigation pertaining to the spot welding of pickled 1/8-, and gritblasted 3/16-, 1/4-, 3/8- and 1/2-in. mild structural steel, the following conclusions may be made:

1. Conditions were established for satisfactorily welding mild structural steel, 1/8-, 3/16-, 1/4-, 3/8- and 1/2 in. in gage thickness.

2. Because of the formation of a brittle structure about the periphery of the weld nuggets in 1/8-, 3/16- and 1/4-in. mild structural steel, the resultant normal tension to shear strength ratios for these materials did not meet the minimum arbitrary limit of 45%.

3. The recommended welding and tempering conditions, which were necessary to increase the normal tension to shear strength to over 45%, have been specified.

4. The sheet separation and indentation measurements for all recommended conditions did not exceed the arbitrary limits of 10 and 5%, respectively.

5. Vickers hardness traverse studies tended to substantiate the selection of the recommended tempering conditions for 1/8-, 3/16- and 1/4-in. mild structural steel.

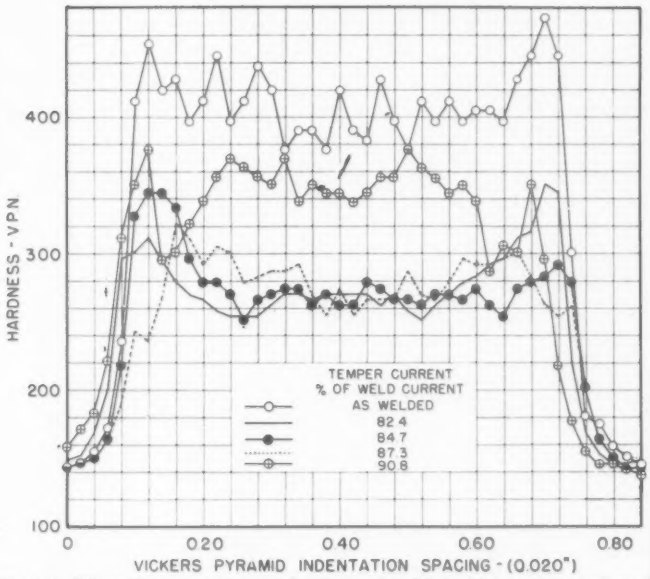

Fig. 20 Effect of tempering upon hardness of welds in 1/8-in. structural steel

Appendix

Supplementary to the establishment of optimum spot-welding conditions, an investigation was conducted to compare the effects of pulsation welding and continuous welding upon the electrode-to-metal interface temperature during the spot welding of 3/8-in. gritblasted mild structural steel. The chemical composition and mechanical properties of the material were the same as given in Table 1.

The method of determining the electrode-to-metal interface temperature during the spot welding of 3/8- x 3- x 3-in. gritblasted and wire-brushed samples was that devised at Rensselaer Polytechnic Institute during the previous investiga-

Table 14—Optimum Welding Conditions

	Gauge Thickness				
	1/8"	3/16"	1/4"	3/8"	1/2"
Welding Electrodes					
Tip Diameter-in.	0.70	0.80	0.875	0.937	1.125
Dome Radius-in.	10	10	10	10	10
Truncated Cone Taper-(degrees)	30	30	30	30	30
Preheat Force-lbs.	----	----	-----	-----	13,500
Welding Force-lbs.	6,000	6,000	6,500	6,500	6,500
Preheat Current-amps	----	----	-----	-----	20,000
Welding Current-amps	28,200	32,000	25,800	26,000	31,100
Preheat Time-cycles	----	----	-----	-----	200
Welding Time-cycles	60	60	240	360	400
Forging Force-lbs.	6,000	10,000	12,500	13,500	13,500
Cool Time-cycles	200	240	240	-----	----
Temper Current-% of Welding Current, amps.	92-95	91-95	87-92	-----	----
Temper Time-cycles	120	120	480	-----	----
Weld Diameter-in (nominal)	0.63	0.71	0.81	0.88	1.03
Normal Tension Strength-lbs. (nominal)	5,850	9,500	15,000	15,700	26,500
Shear Strength - lbs. (nominal)	12,120	19,100	29,000	33,780	45,600
Normal Tension/Shear Strength Ratio - %	48.2	49.7	51.7	46.5	58.2

Table 15—Electrode-Metal Interface Temperature Results for 3/8-in. Gritblasted Mild Structural Steel

(10 cycles on, 10 cycles off, 7½ sec. total weld time)

Group	Sample	Welding Current Amperes	Electrode-Metal Interface Temperature °F	Weld Diameter Inches	Coolant Temperature °F
I	1	33,860	848	0.68	52.5
	2	35,710	890	0.78	
	3	37,120	900	0.81	
	4	38,360	954	0.86	
	5	39,490	922	0.94	
	6	40,510	918	0.94	
	7	41,860	946	0.96	
II	1	33,860	822	0.72	54.5
	2	35,330	846	0.74	
	3	37,120	877	0.89	
	4	38,910	912	0.90	
	5	39,940	931	0.90	
	6	40,510	928	0.94	
	7	41,980	963	0.93	
III	1	34,050	803	0.68	50.0
	2	35,840	845	0.82	
	3	37,500	845	0.83	
	4	39,100	880	0.89	
	5	40,100	900	0.91	
	6	40,830	874	0.93	
	7	41,730	877	0.90	
IV	1	33,860	849	0.71	53.0
	2	35,840	882	0.79	
	3	37,380	882	0.81	
	4	39,100	898	0.92	
	5	39,870	927	0.87	
	6	40,740	946	0.90	
	7	41,990	974	0.96	

Table 16—Electrode-Metal Interface Temperature Results for 3/8-in. Gritblasted Mild Structural Steel, 50% Duty Cycle

(20 cycles on, 20 cycles off, 7½ sec. total weld time)

Group	Sample	Welding Current Amperes	Electrode-Metal Interface Temperature °F	Weld Diameter Inches	Coolant Temperature °F
I	1	34,620	815	0.69	57
	2	35,330	863	0.76	
	3	37,120	873	0.79	
	4	38,270	898	0.84	
	5	39,280	1,013	0.90	
	6	40,380	975	0.96	
	7	41,470	991	0.96	
II	1	34,430	866	0.75	62
	2	35,580	895	0.78	
	3	37,120	905	0.88	
	4	38,140	915	0.87	
	5	39,240	921	0.94	
	6	40,190	921	0.92	
	7	41,730	998	0.97	
III	1	34,690	868	0.71	54
	2	35,970	900	0.74	
	3	37,120	890	0.82	
	4	38,340	897	0.88	
	5	39,230	928	0.92	
	6	40,380	960	0.91	
	7	41,340	967	0.94	
IV	1	34,820	826	0.68	57
	2	35,650	861	0.64	
	3	37,180	890	0.84	
	4	38,590	903	0.84	
	5	39,820	893	0.88	
	6	40,640	935	0.95	
	7	41,340	938	0.94	

* Samples, (3/8 x 3 x 3 in.) wire brushed before being welded in order to remove adhering rust.

Table 17—Electrode-Metal Interface Temperature Results for 3/8-in. Gritblasted Mild Structural Steel, 50% Duty Cycle

(29 cycles on, 29 cycles off, 7½ sec. total weld time)

Group	Sample	Welding Current Amperes	Electrode-Metal Interface Temperature °F	Weld Diameter Inches	Coolant Temperature °F
I	1	35,200	871	0.71	50
	2	37,310	922	0.77	
	3	39,230	932	0.86	
	4	41,150	958	0.85	
	5	42,880	961	0.94	
	6	43,900	961	0.92	
	7	45,700	989	1.01	
II	1	35,290	813	0.60	55
	2	37,630	871	0.80	
	3	39,420	874	0.81	
	4	41,540	906	0.91	
	5	43,400	935	0.95	
	6	44,420	935	0.98	
	7	45,440	954	1.01	
III	1	35,140	836	0.72	58
	2	37,500	874	0.81	
	3	39,300	894	0.81	
	4	41,220	916	0.88	
	5	42,940	948	0.92	
	6	44,290	989	0.96	
	7	46,020	1,040	1.02	
IV	1	35,460	861	0.71	58.5
	2	37,630	903	0.80	
	3	39,170	906	0.89	
	4	41,440	941	0.88	
	5	43,480	951	0.95	
	6	44,800	948	0.97	
	7	46,020	989	1.02	

Table 18—Electrode-Metal Interface Temperature Results for 3/8-in. Gritblasted Mild Structural Steel, 100% Duty Cycle

(7½ sec. total weld time)

Group	Sample	Welding Current Amperes	Electrode-Metal Interface Temperature °F	Weld Diameter Inches	Coolant Temperature °F
I	1	22,720	842	0.68	55
	2	23,680	845	0.74	
	3	25,090	903	0.85	
	4	26,180	942	0.88	
	5	27,900	970	0.89	
	6	29,570	980	0.99	
	7*	30,530	999	1.02	
II	1	22,910	803	0.70	59
	2	24,000	845	0.78	
	3	25,960	891	0.81	
	4	26,110	900	0.88	
	5*	27,710	935	0.93	
	6	29,630	923	1.00	
	7	30,210	967	1.01	
III	1	22,788	895	0.73	54
	2	23,680	908	0.75	
	3*	25,090	930	0.92	
	4*	26,240	939	0.89	
	5	28,030	1,026	0.98	
	6	29,700	1,007	0.98	
	7*	30,666	1,035	0.99	
IV	1	22,780	850	0.62	56.5
	2	23,680	863	0.77	
	3	25,960	902	0.81	
	4	26,300	924	0.89	
	5	27,840	959	0.93	
	6	29,440	994	0.96	
	7*	30,590	958	1.03	

* Irregularly shaped welds.

tion.[8] In order to compare the maximum interface temperature measurements obtained by both pulsation and continuous welding, weld diameter measurements were made across the transverse section of each weld from which an interface temperature measurement had been recorded. The weld diameter measurements corresponding to the interface temperatures resulting from pulsation and continuous welding temperatures were plotted and are shown by Figs. 25–28. A tabulation of these results is given in Tables 15–18.

The welding conditions used for making these tests were:

Welding Machine: 175-kva. Federal press-type welder
Welding Force: 6700 lb.
Electrodes: R.W.M.A. Classification—Group A, Class 2
  Lower Electrode (containing thermocouple)
    Tip Diameter: 1.125 in.
    Dome Radius: 10 in.
    Truncated Cone Taper: 30°
  Upper Electrode
    Tip Diameter: 1.125 in.
    Dome Radius, in.: none (flat)
    Truncated Cone Taper: 3.0°
Weld Time, Cycles:

Pulsation, 50% Duty Cycle

On	Off	Total
10	10	450
20	20	460
29	29	435

Continuous, 100% Duty Cycle: Total 450

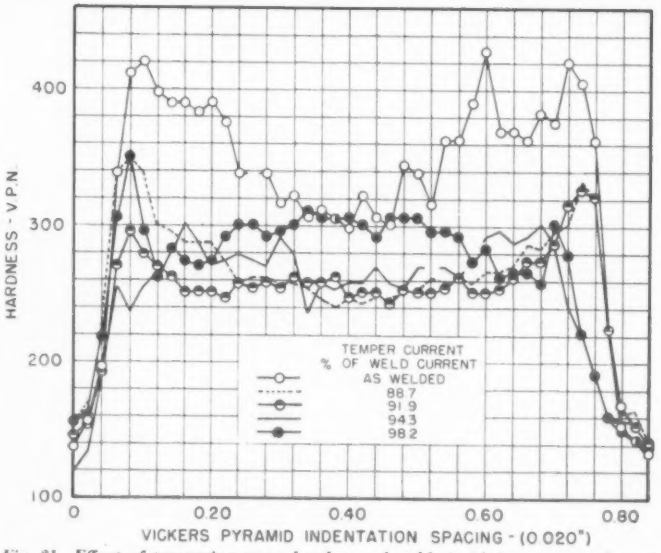

Fig. 21 Effect of tempering upon hardness of welds in $^1/_8$-in. structural steel

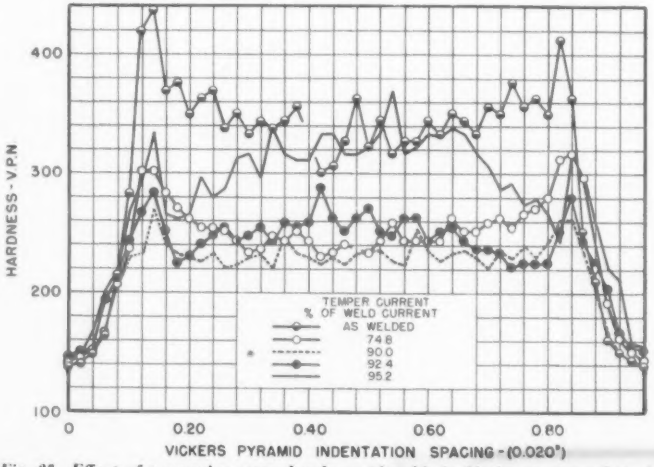

Fig. 23 Effect of tempering upon hardness of welds in $^3/_{16}$-in. structural steel

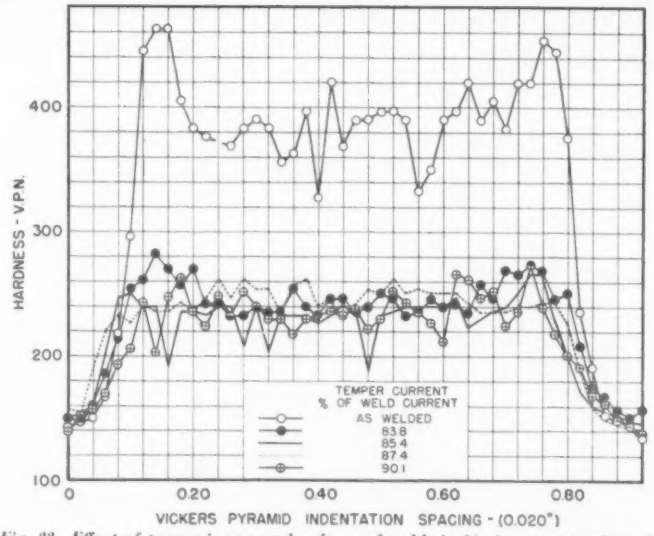

Fig. 22 Effect of tempering upon hardness of welds in $^3/_{16}$-in. structural steel

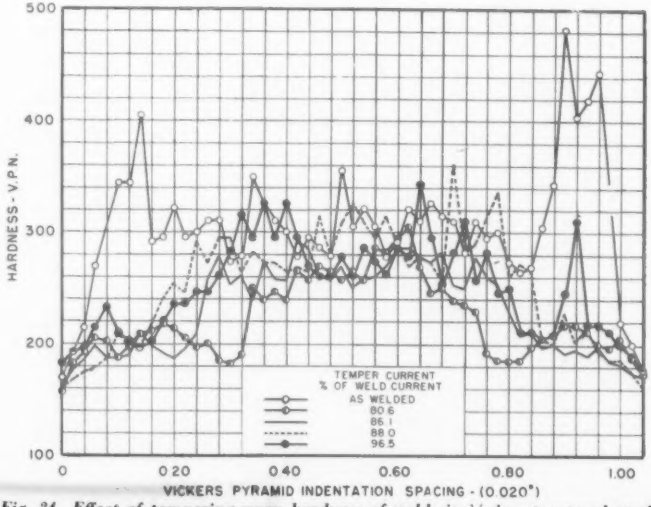

Fig. 24 Effect of tempering upon hardness of welds in $^1/_4$-in. structural steel

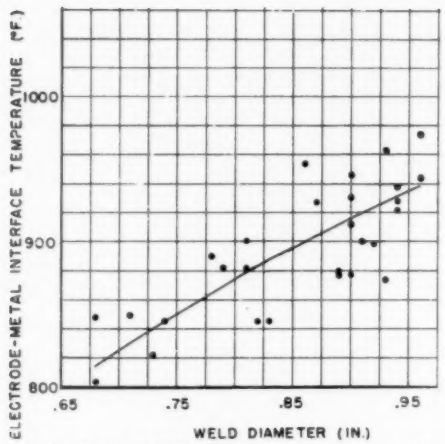

Fig. 25 Effect of 50% duty cycle (10 cycles on, 10 cycles off) upon electrode-metal interface temperature

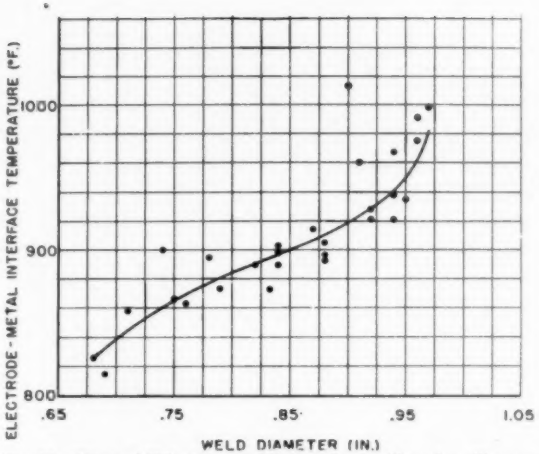

Fig. 26 Effect of 50% duty cycle (20 cycles on, 20 cycles off) upon electrode-metal interface temperature

From a comparison of Figs. 25, 26 and 27 with Fig. 28, it is found that there is no significant difference between the electrode-to-metal interface temperature for spot welds made according to pulsation or continuous welding practice. Although there is some scatter of the points on the various graphs, two factors should be taken into consideration when evaluating the results. First, the deviation of the four curves at any one weld diameter is not more than 20–30° F. Secondly, since the weld diameters are a measure of only one diameter rather than an average of two measurements taken 90° apart, these measurements may not be entirely accurate. If further work is proposed to substantiate the fact that electrode-to-metal interface temperature is comparatively the same when produced by pulsation or continuous welding, it is suggested that a method be determined for measuring two weld diameters normal to each other, for each of the test welds. Then the plot of the interface temperatures vs. weld diameters may give greater consistency than that which has been obtained by this investigation.

The conclusion which is to be drawn from these results is that there is little difference in the electrode-to-metal interface temperatures produced by continuous or pulsation welding for comparable total welding times and spot-weld diameters.

ACKNOWLEDGMENTS

The authors wish to thank the Carnegie-Illinois Steel Corp. for supplying the mild structural steel for this investigation. Also, they are indebted to the P. R. Mallory Co. for the resistance-welding electrode material.

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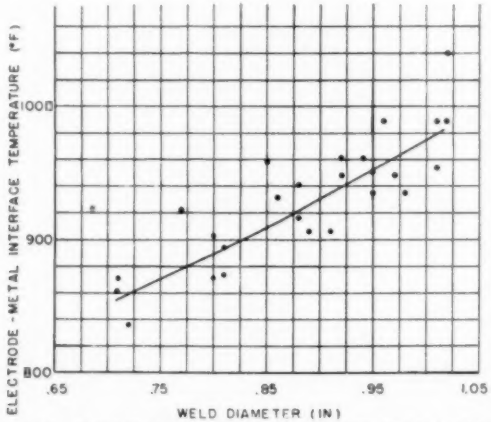

Fig. 27 Effect of 50% duty cycle (29 cycles on, 29 cycles off) upon electrode-metal interface temperature

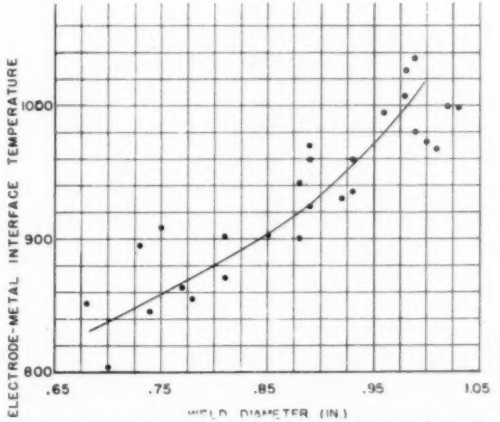

Fig. 28 Effect of 100% duty cycle (7½ sec.) upon electrode-metal interface temperature (° F.)

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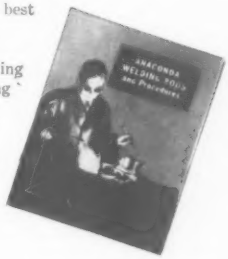

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